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of the

American Foundrymen's Society

Proceedings of the

Fifty - Fourth Annual Meeting

Cleveland, Ohio

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OFFICERS

of the

American Foundrymen's Society (Incorporated)

(Fiscal Year Ending June 30, 1951)

*President, Walton L. Woody, National Malleable & Steel Castings Co., Cleveland, Ohio

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JAMES THOMSON, Continental Foundry & Machine Co., East Chicago, Ind.

E. C. TROY, Foundry Engineer, Palmyra, N. J.

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Headquarters, 616 S. Michigan Ave., Chicago 5, Ill.

Secretary-Treasurer, WM. W. MALONEY

Technical Director, S. C. MASSARI

[•] Member, Executive Committee

SUMMARY OF PROCEEDINGS OF THE 54th ANNUAL MEETING

THE 54TH ANNUAL CONVENTION of the American Foundrymen's Society was held in Cleveland, May 8-12, 1950. The Biennial Foundry and Allied Industries Show was staged by the Society in conjunction with the Convention. Exhibits were displayed at the Cleveland Public Auditorium with over 260 companies showing their products.

Credit for the excellent reception and hospitality received by those attending is given to the Northeastern Ohio Chapter of A.F.S. under the chairmanship of Wm. G. Gude, Penton Publishing Co. This committee excelled in its splendid work and hospitality as host to the thousands of members and guests attend-

ing.

Approximately 12,000 foundrymen, metallurgists, and representatives from allied industries throughout the United States and from 18 countries outside of continental United States attended the Convention. During the five days of the meeting, over 50 sessions were held for presentation of almost 100 timely technical papers and committee reports. Two Nonferrous Shop Course sessions, three Gray Iron Shop Course sessions and three Sand Shop Course sessions were held. A Round-Table Luncheon was sponsored by each Division. A session on Nodular Graphite Gray Iron was sponsored jointly by the Gray Iron and the Malleable Divisions. The Charles Edgar Hoyt Lecture was delivered by W. W. Levi who spoke on "Operation of the Cupola."

A summary of the sessions held follows:

ALUMINUM AND MAGNESIUM SESSION Monday, May 8, 10:00 A.M.

Presiding—R. F. Thomson, International Nickel Co., Detroit. Co-Chairman—C. E. Nelson, The Dow-Chemical Co., Midland, Mich.

Fluid Flow in Transparent Molds—Second Progress Report and Motion Picture, Aluminum and Magnesium Research Project—L. W. Eastwood, Battelle Memorial Institute, Columbus, Ohio.

> Brass and Bronze Session Monday, May 8, 10:00 A.M.

Presiding-J. J. Curran, Walworth Co., Greensburg, Pa. Co-Chairman-A. K. Higgins, Allis-Chalmers Mfg. Co., Milwaukee.

An Investigation of Melting and Casting Procedures for High Purity Nickel-D. W. Grobecker, Los Alamos Scientific Laboratory, Los Alamos, New Mexico.

Metal Melting—Application of Thermodynamic Principles to Melting Nonferrous Metals—R. I. Moore, Wilson Brass and Aluminum Foundry Co., Toronto, Canada.

MALLFABLE SESSION

Monday, May 8, 10:00 A.M.

Presiding-H. C. Stone, Belle City Malleable Iron Co., Racine, Wis.

Co-Chairman-E. Roby, Jr., Peoria Malleable Casting Co., Peoria, Ill. Some Effects of Deoxidizing Additions on Malleable Irons-R. W. Heine, University of Wisconsin, Madison.

Subcritical Isothermal Graphitization—H. A. Schwartz, J. D. Hedberg and R. Eriksen, National Malleable and Steel Castings Co., Cleveland.

ALUMINUM AND MAGNESIUM ROUND TABLE LUNCHEON Monday, May 8, 12:00 Noon

Presiding—Walter Bonsack, Christiansen Corp., Chicago. Co-Chairman—H. J. Rowe, Aluminum Company of America, Pittsburgh.

Subject-Test Bar Gating.

Brass and Bronze Session

Monday, May 8, 2:00 P.M.

Presiding—G. J. Barker, University of Wisconsin, Madison.

Co-Chairman—H. J. Roast, Consultant, Westmont, Quebec.

Effect of Superheat and Casting Temperature on Properties of Tin Bronze-B. N. Ames and N. A. Kahn, New York Naval Shipyard, Brooklyn.

Melt Quality and Fracture Characteristics of 85-5-5-5-Progress Report Brass and Bronze Research Project, F. M. Baker, C. Upthegrove and F. B. Rote, University of Michigan, Ann Arbor.

MALLEABLE SESSION

Monday, May 8, 2:00 P.M.

Presiding-F. W. Jacobs, Lake City Malleable Iron Co., Ashtabula, Ohio.

Co-Chairman-R. P. Schauss, Illinois Clay Products Co., Chicago.

Furnace Atmosphere for Malleable Annealing-W. D. Mc-Millan, International Harvester Co., Chicago.

Surface Hardening of Pearlitic Malleable Iron—A.F.S.-Sponsored Research Project—S. H. Bush, W. P. Wood and F. B. Rote, University of Michigan, Ann Arbor.

PRECISION INVESTMENT CASTING SESSION

Monday, May 8, 2:00 P.M.

Presiding-L. E. Grubb, International Nickel Co., Bayonne,
N. I.

Co-Chairman—T. E. Moore, Ransom and Randolph Co., Toledo.

Casting Aluminum in Moist Investment Molds-S. Lipson and

H. Rosenthal, Frankford Arsenal, Philadelphia.

Pattern Materials and Production in Precision Investment
Casting—E. I. Valyi, A. R. D. Corporation, New York City.

ALUMINUM AND MAGNESIUM SESSION Monday, May 8, 4:00 P.M.

Presiding-M. E. Brooks, The Dow Chemical Co., Bay City,

Co-Chairman—E. J. Vargo, Wellman Bronze & Aluminum Co., Cleveland.

Magnesium Foundry Practice in Canada-M. W. Martinson and J. W. Meier, Bureau of Mines, Ottawa, Canada.

Equipment for Degassing Magnesium Alloy Melts—A. J. Juroff, Bendix Products Div., Bendix Aviation Corp., South Bend, Ind. — (Presently with Duramold Castings, Inc., Mishawaka, Ind.)

CHAPTER OFFICERS AND DIRECTORS DINNER

Monday, May 8, 7:00 P.M.

Presiding—E. W. Horlebein, President, American Foundrymen's Society.

Speaker-R. L. Lee, General Motors Corporation, Detroit.

GRAY IRON SHOP COURSE

Monday, May 8, 8:00 P.M.

Presiding-K. H. Priestley, Vassar Electroloy Products, Inc., Vassar, Mich.

Co-Chairman-E. J. Burke, Hanna Furnace Corp., Buffalo, N. Y.

Subject-Pouring Practice for Gray Iron.

Discussion Leader-E. W. Deutschlander, Worthington Pump Co., Buffalo, N. Y.

Nonferrous Shop Course

Monday, May 8, 8:00 P.M.

Presiding—J. L. Stroman, Stroman Furnace & Engineering Co., Franklin Park, Illinois.

Subject-Fuel Melting.

Discussion Leader-M. G. Dietl, Crane Co., Chicago.

SAND SHOP COURSE

Monday, May 8, 8:00 P.M.

Presiding—F. S. Brewster, Harry W. Dietert Co., Detroit. Co-Chairman—E. L. Thomas, Cadillac Motor Car Div., GMC, Detroit.

Subject-Core and Mold Blowing.

Discussion Panel—A. E. Czechowicz, The Key Co., E. St. Louis, Ill.; R. W. Gardner, Ford Motor Co., Dearborn, Mich.; L. D. Pridmore, International Molding Machine Co., LaGrange Park, Illinois.

ALUMINUM AND MAGNESIUM SESSION

Tuesday, May 9, 10:00 A.M.

Presiding-R. E. Ward, Eclipse-Pioneer Div., Bendix Aviation Corp., Teteboro, N. J.

Co-Chairman-W. E. Carver, Light Metals, Inc., Indianapolis, Indiana.

Characteristics of Some Aluminum-Zinc-Magnesium-Copper Casting Alloys—W. E. Sicha and H. Y. Hunsicker, Aluminum Co. of America, Cleveland.

Corrosion and Stress-Corrosion Properties of a High Strength Aluminum-Zinc-Magnesium-Copper Casting Alloy-R. A. Quadt and E. C. Reichard, Federated Metals Div., American Smelting & Refining Co., Barber, N. J.

BRASS AND BRONZE SESSION

Tuesday, May 9, 10:00 A.M.

Presiding-W. Romanoff, H. Kramer & Co., Chicago.

Co-Chairman-G. K. Eggleston, Barnes Mfg. Co., Mansfield, Ohio.

Metal Specifications for Brass and Bronze Foundry-J. G. Dick, Canadian Bronze Co., Ltd., Montreal, Canada.

Aluminum Additions in Commercial Yellow Brass-R. A. Colton and R. H. Gilbert, American Smelting & Refining Co., Barber, N. J.

HEAT TRANSFER SESSION

Tuesday, May 9, 10:00 A.M.

Presiding-H. A Schwartz, National Malleable & Steel Castings Co., Cleveland.

Co-Chairman-E. C. Troy, Foundry Engineer, Palmyra, N. J. Solidification of Ingots-B. H. Alexander, Sylvania Electric Products, Inc., Bayside, L. I., N. Y.

Influence of Dry Sand Conductivity on Rate of Freezing of Steel Slabs-Progress Report, Heat Transfer Research Project, V. Paschkis, Columbia University, New York.

MALLEABLE SESSION

Tuesday, May 9, 10:00 A. M.

Presiding-H. Hunt, Auto Specialties Mfg. Co., St. Joseph, Mich.

Co-Chairman-F. Wurscher, Marion Malleable Iron Co., Marion, Ind.

Inspection of Pearlitic Malleable Castings—D. T. Martin, Saginaw Malleable Iron Plant, Central Foundry Div., GMC, Saginaw, Michigan.

Quantitative Correlation of Factors Influencing Annealing— R. Schneidewind, University of Michigan, Ann Arbor, Mich.

BRASS AND BRONZE ROUND TABLE LUNCHEON

Tuesday, May 9, 12:00 Noon

Presiding-B. A. Miller, Baldwin Locomotive Works, Philadelphia.

Co-Chairman-H. L. Smith, Federated Metals Div., American Smelting and Refining Co., Pittsburgh.

Subject-Casting Defects.

Discussion Leader-H. F. Taylor, Massachusetts Institute of Technology, Cambridge, Mass.

MALLEABLE ROUND TABLE LUNCHEON

Tuesday, May 9, 12:00 Noon

Presiding-J. H. Lansing, Malleable Founders' Society, Cleveland.

Co-Chairman-C. F. Lauenstein, Link-Belt Co., Indianapolis, Ind.

Subjects-(1) Effect of Excessive Oxidation in Duplex Melting,

(2) Design of Dies for Straightening.

Discussion Panel-Milton Tilley, National Malleable & Steel

Discussion Panel—Milton Tilley, National Malleable & Steel Castings Co., Cleveland; H. C. Stone, Belle City Malleable Iron Co., Racine; H. G. Sieggreen, Central Foundry Div., GMC, Saginaw; K. L. Sanders, Albion Malleable Iron Co., Albion, Mich.

ALUMINUM AND MAGNESIUM SESSION

Tuesday, May 9, 2:00 P.M.

Presiding—S. C. Massari, American Foundrymen's Society, Chicago.

Co-Chairman—H. Brown, Solar Aircraft Co., Des Moines, Iowa.

Aluminum Alloy Castings—A Review of British Achievement—
Frank Hudson, Mond Nickel Co., London, England. Official Exchange Paper from Institute of British Foundrymen.

SAND SESSION

Tuesday, May 9, 2:00 P.M.

Presiding—H. K. Salzberg, The Borden Co., Bainbridge, N. Y. Co-Chairman—E. E. Woodliff, Foundry Sand Service Engineering Co., Detroit.

Treatment of Bond Clays for Foundry Sand—A. E. Pavlish (formerly with Battelle Memorial Institute, Columbus), Kelly Island Lime and Transport Co., Cleveland—(Presented by H. W. Lownie, Battelle Memorial Institute, Columbus).

Phenolic Resin Core Binders-Effect of Core Mix on Physical Properties-J. E. McMillan and J. A. Wickett, Monsanto Chemical Co., Springfield, Mass.

ALUMINUM AND MAGNESIUM SESSION

Tuesday, May 9, 4:00 P.M.

Presiding-R. T. Wood, American Magnesium Corp., Cleveland.

Co-Chairman-J. C. McDonald, The Dow Chemical Co., Midland, Mich.

Casting of Magnesium-Rare Earth-Zirconium Alloys in Sand-K. E. Nelson and F. P. Strieter, The Dow Chemical Co., Midland, Mich

Development of High-Strength Magnesium Casting Alloy ZK61-J. W. Meier and M. W. Martinson, Bureau of Mines, Ottawa, Canada.

EDUCATIONAL SESSION

Tuesday, May 9, 4:00 P.M.

Presiding—A. W. Gregg, Whiting Corporation, Harvey, Illinois. Co-Chairman—W. H. Ruten, Brooklyn Polytechnic Institute, trooklyn.

Round Table Discussion-High School and Trade School Training.

Discussion Panel-W. J. MacNeill, Dayton Malleable Iron Co., Dayton; J. H. Smith, Central Foundry Div., GMC, Saginaw; P. E. Rentschler, Hamilton Foundry & Machine Co., Hamilton, Ohio.

PATTERN SESSION

Tuesday, May 9, 4:00 P.M.

Presiding—J. Mathias, Accurate Matchplate Co., Chicago. Co-Chairman—A. F. Pfeiffer, Allis-Chalmers Mfg. Co., Milwaukee.

Making Pressure-Cast Aluminum Match Plates in Plaster Molds-K. A. Miericke, Baroid Sales Div., National Lead Co., Chicago.

Production Patterns and the Matchplate-R, F. Dalton, U. S. Gypsum Company, Chicago.

Cold-Formed Flexible Precision Patterns and Core Boxes— J. E. Wiss and R. B. Wagner, Perma-Flex Mold Co., Columbus, Ohio.

CANADIAN DINNER

Tuesday, May 9, 7:00 P.M.

Presiding-E. N. Delahunt, Warden King, Ltd., Montreal. Co-Chairman-J. J. McFadyen, Galt Malleable Iron Co., Galt, Ont.

EDUCATIONAL DINNER

Tuesday, May 9, 7:00 P.M.

Presiding—B. D. Claffey, Acme Aluminum Alloys, Inc., Dayton. Co-Chairman—G. K. Dreher, Foundry Educational Foundation. Cleveland.

Foundry Apprentice Training at Caterpillar Tractor Co.-F. W. Shipley and B. L. Bevis, Caterpillar Tractor Co., Peoria, Illinois.

The Engineering Student and Summer Foundry Work-N. J. Stickney, University of Wisconsin, Madison.

GRAY IRON SHOP COURSE

Tuesday, May 9, 8:00 P.M.

Presiding-E. J. Burke, Hanna Furnace Corp., Buffalo, N. Y. Subject-Cupola Bed Practice.

Discussion Leader-B. P. Mulcahy, Consultant, Indianapolis. Ind.

Nonferrous Shop Course

Tuesday, May 9, 8:00 P.M.

Presiding-B. W. Schafer, Kuhlman Electric Co., Bay City, Mich.

Subject-Electric Furnace Melting.

Discussion Leader-M. G. Dietl, Crane Co., Chicago.

SAND SHOP COURSE

Tuesday, May 9, 8:00 P.M.

Presiding-E. J. Bush, U. S. Naval Gun Factory, Washington, D. C.

Co-Chairman-R. H. Olmstead, Whitehead Bros. Co., New York City.

Subject-Core Dip Test.

Discussion Leader-J. B. Caine, Consultant, Wyoming, Ohio.

JOINT GRAY IRON AND MALLEABLE SESSION

Wednesday, May 10, 10:00 A.M. Presiding-H. Bornstein, Deere & Co., Moline, Illinois.

Co-Chairman-T. E. Eagan, Cooper-Bessemer Corp., Grove City, Pa.

Problems in Producing Ductile Iron-Max Kuniansky, Lynchburg Foundry Co., Lynchburg, Virginia.

An Introduction to the Annealing of Nodular Iron-J. E. Rehder, Bureau of Mines, Ottawa, Canada.

REFRACTORIES SESSION

Wednesday, May 10, 10:00 A.M.

Presiding—R. H. Stone, Vesuvius Crucible Co., Pittsburgh. Co-Chairman—R. A. Witschey, A. P. Green Fire Brick Co., Chicago.

Refractory Practice in Melting of Acid Electric Steel-R. H. Jacoby and M. Petty, The Key Co., East St. Louis, Illinois.

SAND SESSION

Wednesday, May 10, 10:00 A.M.

Presiding-C. B. Jenni, General Steel Castings Corp., Eddy-stone, Pa.

Co-Chairman-J. H. Lowe, Wehr Steel Co., Milwaukee.

Reproducibility of Foundry Sand Tests-B. H. Booth, Carpenter Bros., Inc., Milwaukee; P. C. Rosenthal, University of Wisconsin, Madison, and H. W. Dietert, Harry W. Dietert Co., Detroit.

Tenth Annual Report on Investigation of Properties of Steel Sands at Elevated Temperatures-R. G. Thorpe, P. E. Kyle and J. P. Fraser. Cornell University, Ithaca, N. Y.

STEEL SESSION

Wednesday, May 10, 10:00 A.M.

Presiding-C. W. Briggs, Steel Founders' Society of America, Cleveland.

Co-Chairman—R. E. Kerr, Pettibone-Mulliken Corp., Chicago.

Nature of Mold Cavity Gases—Charles Locke and R. L. Ashbrook, Armour Research Foundation, Chicago.

Metal Penetrations in Steel Sands-S. L. Gertsman and A. E. Murton, Bureau of Mines, Ottawa, Canada.

Thermodynamic Study of Pinhole Formations in Steel Castings-H. F. Taylor, Massachusetts Institute of Technology, Cambridge, Mass., and R. E. Savage, International Nickel Co., New York.

GRAY IRON ROUND TABLE LUNCHEON

Wednesday, May 10, 12:00 Noon

Presiding—F. S. Brewster, Harry W. Dietert Co., Detroit. Co-Chairman—R. A. Clark, Electro Metallurgical Div., Union Carbide & Carbon Co., Detroit.

Subject—The Effect of Mold Materials on Metal Shrinkage.
Discussion Leaders—C. C. Sigerfoos, Michigan State College,
E. Lansing; C. A. Sanders, American Colloid Co., Chicago; W.
F. Bohm, Buick Motor Co., Flint, Mich.

PATTERN ROUND TABLE LUNCHEON

Wednesday, May 10, 12:00 Noon

Presiding-G. E. Garvey, City Pattern & Foundry Co., South Bend, Ind.

Co-Chairman-H. C. Swanson, Arrow Pattern & Foundry Co., Chicago.

Subject-An Alloy Designed for Pattern Shops. Discussion Leader-S. Greenfield Co., Buffalo, N. Y.

Annual Business Meeting and Charles Edgar Hoyt Annual Lecture

Wednesday, May 10, 3:00 P.M.

Presiding—E. W. Horlebein, President, American Foundrymen's Society.

President Horlebein called the meeting to order as the Annual Business Meeting of the American Foundrymen's Society. He then presented the President's Annual Address. See page 13.

Following this presentation President Horlebein called on Secretary-Treasurer Maloney who reported on the nominations of Officers and Directors for the coming year and stated that no additional nominees had been received in accordance with the procedure prescribed in Art. X of the Society By-Laws. He therefore cast the unanimous ballot of the membership of A.F.S. for the election of the following:

President (to serve one year):

W. L. Woody, National Malleable & Steel Castings Co., Cleveland.

Vice-President (to serve one year):

W. L. Seelbach, Superior Foundry, Inc., Cleveland.

Directors (to serve three years):

J. J. McFadyen, Galt Malleable Iron Co., Galt, Ont., Canada. J. O. Ostergren, Lakey Foundry & Machine Co., Muskegon, Mich.

F. W. Shipley, Caterpillar Tractor Co., Peoria, Ill. James Thomson, Continental Foundry & Machine Co., East

Chicago, Ind. E. C. Troy, Foundry Engineer, Palymra, N. J.

Director (to serve one year): E. W. Horlebein, Gibson & Kirk Co., Baltimore.

Director (to fill unexpired term of A. M. Fulton, deceased):
M. J. O'Brien, Jr., Symington-Gould Corp., Depew, N. Y.

Following announcement of elections, President Horlebein introduced the newly elected Officers and Directors present. He then called on Secretary-Treasurer W. W. Maloney who announced the 1950 Apprentice Contest winners as follows:

Wood Patternmaking Division (new this year)

1st-Eugene W. Chmielowicz, The Motor Patterns Co., Cleveland.

2nd-James A. Orban, Royal Pattern Works Co., Cleveland. 3rd-Donald A. Siebert, Royal Pattern Works Co., Cleveland.

Metal Patternmaking Division

lst-John W. Burkholder, Jr., Central Pattern Co., St. Louis. 2nd-James V. Gaino, Royal Pattern Works Co., Cleveland. 3rd-John F. Banach, City Pattern & Foundry Co., Inc., South Bend, Ind.

Gray Iron Molding Division

1st-Richard Bowman, American Car & Foundry Co., St. Louis. 2nd-Ernest E. Grenier, Builders Iron Foundry Co., Providence, R. I.

3rd-Taisto Pethman, General Metals Corp., Oakland, Calif.

Steel Molding Division

1st-Richard C. Tracy, Dominion Engineering Works Ltd., Montreal, Canada.

2nd-Guy Belanger, Dominion Engineering Works Ltd., Montreal, Canada.

3rd-Norman G. Bublitz, Bucyrus-Erie Co., So. Milwaukee, Wis.

Nonferrous Molding Division

1st-Worth Cropp, Jr., Pacific Brass Foundry, San Francisco. 2nd-Anthony J. Masiakowski, Nordberg Mfg. Co., Milwaukee. 3rd-Victor J. Gadzalinski, Nordberg Mfg. Co., Milwaukee.

The Society arranged to have the five first-prize winners present at the Convention and to receive their awards in person. The first-prize winners were called to the platform and President Horlebein presented each with the first prize, a check for \$100.00 and an engraved Certificate of Award prefaced by a few words of commendation and encouragement.

President Horlebein then introduced N. P. Newman, President, Institute of British Foundrymen who expressed best wishes on the occasion of the 54th A.F.S. Convention to A.F.S.

from I.B.F.

President Horlebein next introduced John Larsen Sissener of the Norwegian Foundrymen's Technical Society who expressed his gratitude for the hospitality extended him and others from Norway during their stay in Cleveland.

President Horlebein then introduced Frank Hudson, author of the IBF exchange paper to A.F.S. and head of the British

nonferrous productivity team.

President Horlebein then introduced Sven Toresson, chairman of the Goteborg Branch of the Swedish Foundrymen's Association.

President Horlebein then introduced W. W. Levi, Lynchburg Foundry Co., Radford, Virginia, who delivered the Charles Edgar Hoyt Lecture on the subject, "Operation of the Cupola" which begins on page 1 of this volume.

ANNUAL BANQUET

Wednesday, May 10, 7:00 P.M.

Presiding—E. W. Horlebein, President, American Foundrymen's Society.

The Annual A.F.S. Dinner of the Society's 54th Annual Convention was called to order by President E. W. Horlebein, presiding.

Following the singing of the National Anthem by Miss Audrey Norris with audience participation, President Horlebein introduced Past President D. P. Forbes, Chairman of the A.F.S. Board of Awards. After brief preliminary remarks, Past President Forbes introduced Dr. G. H. Clamer who presented the Joseph S. Seaman Gold Medal to Dr. C. H. Lorig.

PRESENTATION OF SEAMAN GOLD MEDAL

Dr. Clamer stated:

"I have the honor to present the Joseph S. Seaman Gold Medal to a candidate having a long record of outstanding accomplishments. He has been and is now an indefatigable worker in the field of metallurgy. His work has covered projects ranging from those of a most simple nature to advanced physical metallurgy. He has been a prolific writer and has presented fourteen papers before the annual meetings of our Society; many papers before our Society chapters and regional conferences.

"Dr. Lorig was born on August 2, 1900 in St. Paul, Minn. He received his early education in South Milwaukee, Wis. and attended the University of Wisconsin, receiving a B. S. degree in 1924, M. S. degree in 1925 and a Ph. D. in 1928. He was employed as metallurgist with the Stowell Co., in South Mil-

waukee 1925-26; the French Battery Co. as research engineer in 1926; as a metallurgist for the Ladish Drop Forge Co., Houdaille, Wis. 1928-29; as an assistant professor at Drexel Institute, Philadelphia in 1929-30 and Supervisor at Battelle Memorial Institute. He is at present Assistant Director at Battelle Memorial Institute. He is a member of six technical societies in the metallurgical and foundry fields and has contributed greatly to the work of each organization.

"Dr. Lorig's most outstanding personal characteristics are his inherent modesty and humility. Next, and nearly as outstanding, is the high personal regard and respect in which he is held by all of the persons with whom he works. The personal interest he takes in these who work for him, and his active championing of their interests, is somewhere at the root of it. In his daily personal contacts he is more of a friend than a boss.

"Some years ago, when the press of work permitted, Dr. Lorig was taking an active part in a research project. Dressed in coveralls, he was operating the forging hammer. A local newspaper reporter was visiting Battelle Memorial Institute at the time, and was somewhat overawed at the thought of a Ph. D. working in that manner, and this was reflected in the story which subsequently appeared in the papers. Needless to say, Dr. Lorig was somewhat embarrassed by the story and its accompanying picture, which his associates considered a compliment to him as an individual.

"Dr. Lerig's mest active hobby is gardening. Perhaps this stems from his boyhood on the farm, but regardless of the source, he is considered to have a 'green thumb.' Some of his flowers are outstanding. It is probably not surprising that he has been President of the Kingswood Garden Club in his community.

"His interest in his two children has also led him into the activities of his community. At present he is a member of the Board of Directors of the Upper Arlington Booster's Club, which actively supports and encourages activities of the Upper Arlington schools. In addition, he serves as a member of the Boy Scout Troop Committee for a local scout troop, and frequently accompanies them on their hikes, and other activities.

"His vacations are usually spent in Michigan and consist of

boating, canoeing, and fishing.

"I had the good fortune to have known Joseph S. Seaman -'Daddy' Seaman, as he was affectionately called by his many friends. I am sure he would have been most happy to know that the Board of Awards has this year chosen such a worthy candidate for the award of his medal. It is with great pleasure, Dr. Lorig, that I present you this large and beautiful gold medal, the gift of a loyal, enthusiastic and valuable member of our Society long years ago. A memory of him is ever recurring when a Joseph S. Seaman Medal is awarded by our Society."

DR. LORIG'S ACCEPTANCE OF SEAMAN MEDAL

Dr. Lorig graciously accepted the Seaman Gold Medal with these words:

"It was just a half century ago that Joseph S. Seaman was the President of this Society. He served it generously when it was young, and under his guidance, its foundations were firmly established. His spirit remains with us in the records of growth and service of our Society. And it is fitting that his spirit is brought again to our attention periodically by means of this award that bears his name.

"It happens, also, that it was just 25 years ago that Dr. Richard Moldenke became the first recipient of the Joseph S. Seaman medal. His desire to make the work done by foundries better through science made him a splendid choice for that honor.

"Seaman, even in his day, seems to have foreseen that science was destined to be a powerful tool of the foundry industry. But even his vision could hardly have foreseen the rapidly expanding frontiers of foundry research today. And tomorrow's frontiers will be even wider.

"Science is the sum of all universal knowledge, but we have as yet acquired only a small portion of it. New knowledge is acquired through research. The way to new knowledge is not easy and few can travel it successfully alone. The important achievements in research, as in industry, are seldom the work of the individual; they come about through the work of many.

"The credit and honor conferred upon me by the award of the Joseph S. Seaman Medal this year, I should like to feel, are symbolic of the credit and honor due others as well who have been associated with me in this Society and in research. In that spirit, I am proud to accept the award. For it, I want to express my sincerest thanks to the Directors, to the Board of Awards, and to the Officers and members of A.F.S. The award is a priceless gift. I shall always cherish it."

PRESENTATION OF WM. H. McFadden Gold Medal

Past President Forbes then introduced Past President Max Kuniansky who presented the William H. McFadden Gold Medal to Prof. Richard Schneidewind with these prefacing remarks:

"It is, indeed, an honor and a privilege for me to have been selected to present the Wm. H. McFadden Gold Medal to Dr. Schneidewind. This year the Board of Awards decided to ask a past recipient of the same gold medal to make the presentation to the current recipient and welcome him into the family of medallists.

"It is, therefore, with a great deal of pleasure that I accept this duty. Dr. Schneidewind has done outstanding work in the field of converting white iron to malleable iron. He has written many papers that have been presented before the American Foundrymen's Society and other technical groups. Even more important than this is the fact that he has, as a member of the teaching profession, served as a source of inspiration and encouragement to many young men who leave school and come into the foundry industry.

"Prof. Schneidewind, it affords me a great deal of pleasure to welcome you into the family of McFadden Gold Medallists. Permit me herewith to present to you the McFadden Gold Medal for 1950, which you have so richly deserved for your

outstanding service to the Foundry Industry."

ACCEPTANCE OF McFadden Medal by Dr. Schneidewind

Prof. Schneidewind graciously accepted the Wm. H. Mc-Fadden Gold Medal with the following words of thanks:

"I am intensely pleased and gratified with the distinction of having been brought into the fellowship of McFadden Medallists. This honor is received with some feeling of humility since most of my work has been done in collaboration with other men, many of whom have wider experience than I. This work has been directed not entirely to the foundry industry but also to students and to the users of cast products. I hope that I shall be worthy of the honor which has been conferred upon me."

PRESENTATION OF HONORARY LIFE MEMBERSHIPS

Past-President Forbes next introduced Dr. H. F. Taylor who presented an Honorary Life Membership to Clyde L. Frear of the Bureau of Ships, Department of the Navy, with the following prefacing remarks:

"An Honorary Life Membership in A.F.S. is not a recognition one obtains by chance. IT MUST BE EARNED. A man is chosen for this distinction from 10,000 potential candidates.

"Perhaps you know Mr. Frear best for his work on radiographic standards for steel castings, or for his many talks and papers on iron, steel, and bronze. He has always given freely of his time for these jobs.

"I know him best as a dependable champion of foundry research at the Naval Research Laboratory. I also know him as the man who pioneered the gray iron and bronze research at

NRL

"Mr. Frear is a product of Syracuse University and of Queens University, Kingston, Ontario. He has been Professor of Chemistry and Metallurgy at Lehigh University and at the Post Graduate School at the U. S. Naval Academy. He has also been Metallurgical Engineer at DeLaval Separator Co. and at Kelsey Hayes Wheel Co. He is now in charge of the Casting Section, Bureau of Ships, U. S. Navy Department.

"On behalf of A.F.S., I am pleased to present to you an

Honorary Life Membership in A.F.S."

ACCEPTANCE BY MR. FREAR

Mr. Frear graciously accepted from Dr. Taylor the Honorary Life Membership Certificate with the following remarks:

"Please accept my sincere appreciation for the honor bestowed upon me. What I may have done to deserve this recognition could not have been accomplished without the cooperation of various Naval activities and the many foundrymen who have supplied castings to the Navy and who evidenced a sincere interest in the improvement of this product."

PRESENTATION BY F. J. WALLS

Past-President Forbes next introduced Fred J. Walls, who presented an Honorary Life Membership to Ralph L. Lee. Mr.

"On behalf of the A.F.S. Board of Awards, it becomes my sincere pleasure to present an Honorary Life Membership to

Ralph L. Lee of General Motors Corp.
"The citation for this award is 'for his philosophical approach to the problems of the foundry industry and for his stimulation of pride of craftsmanship among foundrymen."

"His own greatest pride and pleasure lie in his principal hobby, 'a unique one-man foundry.'

The colorful career of Ralph Lee in the fields of engineering, of sales, and of research is overshadowed by his more recent accomplishments in that unlimited field of 'Humanics.'

"Your book, 'Man to Man on the Job,' your many articles in the technical journals associated with our industry (and other industries), and your inspiring talks before our chapters and conferences are testimonials to the citation for which you are being awarded this Honorary Life Membership in A.F.S. on this occasion of its 54th Annual Meeting."

ACCEPTANCE BY MR. LEE

Ralph L. Lee graciously accepted the Honorary Life Membership Certificate from Fred J. Walls with these remarks:

Next to my marriage certificate, this is the finest thing I have ever received. Please rest assured that I had no idea that it was in prospect. The older I get, the more certain it seems that our most treasured prizes come to us as unexpected surprises rather than a long and hard fought battle to acquire them. I shall keep right on doing my bit for one of the finest technical societies there is and for the even finer folks who go to make it up."

HONORARY LIFE MEMBERSHIP TO PRES. HORLEBEIN

Past President Forbes then presented an Honorary Life Membership Certificate to President E. W. Horlebein on completion of his term of office as President of the Society. Mr. Horlebein accepted the Honorary Life Membership Certificate graciously and expressed his appreciation.

GUEST SPEAKER

Following presentation of Awards, the guest speaker of the evening, Leonard E. Read, President, Foundation for Economic Education, Irvington-on-Hudson, N. Y., spoke on "Students of Liberty" in which he expressed the need ever to be vigilant lest we forfeit the freedom and liberty for which our founding fathers have fought and have written into those immortal documents, the Declaration of Independence and the Constitution of the United States.

GRAY IRON SESSION

Thursday, May 11, 10:00 A.M.

Presiding-A. P. Gagnebin, International Nickel Co., Bayonne,

Co-Chairman-W. B. McFerrin, Haynes Stellite Div., Kokomo,

Improved Test Bars for Standard and Ductile Grades of Cast Iron-R. A. Flinn and R. W. Kraft, American Brake Shoe Co., Mahwah, N. J.

Report of Cupola Research Committee-R. G. McElwee, Vana-

dium Corporation of America, Detroit.

MANAGEMENT FUNCTIONS AND CONTROLS SESSION Thursday, May 11, 10:00 A.M.

Presiding-R. L. Lee, Grede Foundries, Inc., Milwaukee. Co-Chairman-V. E. Zang, Unitcast Corporation, Toledo. Administration of Wage Incentives-C. T. Hassell, Lester B. Knight and Associates, Chicago.

Organizing Management for Profit-W. E. George, Booz, Allen and Hamilton, Chicago.

SAND SESSION

Thursday, May 11, 10:00 A.M.

Presiding-B. H. Booth, Carpenter Bros., Inc., Milwaukee. Co-Chairman-J. O. Ochsner, Crouse-Hinds Co., Syracuse, N. Y.

Flowability of Molding Sands-W. H. Moore, Meehanite Metals Corporation, South Euclid, Ohio.

Factors Affecting Molding Sand Density, Shrinkage and Workability-R. P. Schauss, R. F. Baley, Illinois Clay Products Co., Chicago, and E. E. Woodliff, Foundry Sand Service Engineering Co., Detroit.

STEEL SESSION

Thursday, May 11, 10:00 A.M.
Presiding—R. C. Wood, Minneapolis Electric Steel Castings Co., Minneapolis.

Co-Chairman-D. C. Zuege, Sivyer Steel Casting Co., Mil-

Influence of Temperature on Fluidity and Surface Appearance of Steel Castings-G. A. Lillieqvist, American Steel Foundries, East Chicago, Indiana.

Steel Quality as Related to Test Bar Fractures-H. H. Johnson and C. A. Fisher, National Malleable & Steel Castings Co., Sharon, Pa.

STEEL ROUND TABLE LUNCHEON

Thursday, May 11, 12:00 Noon

Presiding-V. E. Zang, Unitcast Corporation, Toledo.

Co-Chairman-R. E. Kerr, Pettibone-Mulliken Corp., Chicago. Subjects-(1) Fluidity Tests in Relation to Temperature and Temperature Control; (2) Steel Temperature Measurement; (3) Casting Surface Finish and Defects.

GRAY IRON SESSION

Thursday, May 11, 2:00 P.M.

Presiding-A. E. Schuh, United States Pipe & Foundry Company, Burlington, N. J.

Co-Chairman-C. O. Burgess, Gray Iron Founders' Society, Cleveland.

Composition and Properties of Gray Iron-R. Schneidewind, University of Michigan, Ann Arbor, and R. G. McElwee, Vanadium Corporation of America, Detroit.

Basic-Lined Cupola-S. F. Carter, American Cast Iron Pipe Company, Birmingham, Ala.

SAND SESSION

Thursday, May 11, 2:00 P.M.

Presiding-I. A. Rassenfoss, American Steel Foundries, E. Chicago, Ind.

Co-Chairman-P. E. Kyle, Cornell University, Ithaca, N. Y. A Study of the Effect of Various Binders and Additives on the Hot Strength of Molding Sand-R. E. Morey and C. G. Ackerlind, Naval Research Laboratory, Washington, D. C.

Effect of Moisture on Core Sand Mixtures-O. J. Myers, Werner G. Smith Company, Minneapolis.

TIMESTUDY AND METHODS SESSION

Thursday, May 11, 2:00 P.M.

Presiding-E. G. Tetzlaff, Pelton Steel Casting Co., Milwaukee. Co-Chairman-M. T. Sell, Sterling Foundry Co., Wellington,

Standard Data for Bench Coremaking-R. H. Williams, Williams Management Engineering Co., Milwaukee.

Fatigue Data Survey-M. E. Annich, American Brake Shoe Co., Mahwah, N. J.

FOUNDRY COST SESSION

Thursday, May 11, 4:00 P.M.

Presiding-R. L. Lee, Grede Foundries, Inc., Milwaukee. Co-Chairman-G. Tisdale, Zenith Foundry Co., Milwaukee. Management Views Costs-J. A. Wagner, Wagner Malleable Iron Co., Decatur, Illinois.

PLANT AND PLANT EQUIPMENT SESSION

Thursday, May 11, 4:00 P.M.

Presiding-J. Thomson, Continental Foundry & Machine Company, East Chicago, Indiana.

Co-Chairman-R. J. Wolf, Stone & Webster Engineering Corp., Boston.

Oxygen Cutting Processes in Steel Foundries-R. Babcock, The Linde Air Products Co., Development Div., Newark, N. J.

Bandsawing in Foundries-G. H. Sheppard, The DoALL Co., Des Plaines, Illinois.

Selection and Application of Cleaning Equipment—S. F. Krzeszewski, American Wheelabrator & Equipment Co., Mishawaka, Indiana.

JOINT SAND AND STEEL SYMPOSIUM

Thursday, May 11, 4:00 P.M.

Presiding-C. Locke, Armour Research Foundation, Chicago. Co-Chairman-C. B. Jenni, General Steel Castings Corp., Eddystone. Pa.

Subject-Interpretation of Sand Tests as Related to Steel Castings.

Introduction-E. C. Troy, National Engineering Co., Palmyra, N. J.

Room Temperature Testing-J. H. Lowe, Wehr Steel Co., Milwaukee; W. J. Downey, Burnside Steel Foundry Co., Chicago.

High Temperature Testing-P. E. Kyle, Cornell University, Ithaca, N. Y.; R. H. Jacoby, The Key Co., E. St. Louis, Ill.; H. W. Meyer, General Steel Castings Corp., Granite City, Ill.; J. A. Rassenfoss, American Steel Foundries, East Chicago, Ind.; J. B. Caine, Wyoming, Ohio.

A.F.S. ALUMNI DINNER Thursday, May 11, 7:00 P.M.

GRAY IRON SHOP COURSE

Thursday, May 11, 8:00 P.M.

Presiding-H. H. Wilder, Foundry Div., Eaton Mfg. Co., Vassar, Mich.

Co-Chairman-E. J. Burke, Hanna Furnace Corp., Buffalo, N. Y.

Subject-Cupola Refractory Practice.

Discussion Panel-E. J. Lally, Forest City Foundries Co., Cleveland; T. Giszczak, Research Met., A.F.S., Detroit.

PLANT AND PLANT EQUIPMENT SESSION

Thursday, May 11, 8:00 P.M.

Presiding-J. Thomson, Continental Foundry & Machine Co., East Chicago, Indiana.

Co-Chairman-H. W. Johnson, Wells Mfg. Co., Skokie, Illinois. Foundry Cleaning Room Abrasive Operations-B. H. Work, The Carborundum Co., Niagara Falls, N. Y.

Salvage of Castings by Welding of Defects-G. E. Bellew, Air Reduction Sales Co., New York City.

Use of Portable Air Tools in Foundry Cleaning Rooms-A. G. Ringer, Ingersoll-Rand Co., New York City.

Material Transport in the Cleaning Room-N. L. Smith, Link-Belt Co., New York City, and R. J. Wolf, Stone & Webster Engrg. Corp., Boston.

SAND SHOP COURSE

Thursday, May 11, 8:00 P.M.

Presiding-R. H. Jacoby, The Key Co., East St. Louis, Illinois. Co-Chairman-H. W. Meyer, The General Steel Castings Co., Granite City, Illinois.

Subject-Sand Reclamation.

Discussion Leader-G. H. Curtis, Nichols Engineering & Research Co., New York City.

GRAY IRON SESSION

Friday, May 12, 10:00 A.M.

Presiding-F. J. Walls, International Nickel Co., Detroit. Co-Chairman-G. A. Timmons, Climax Molybdenum Co.,

Correlation of Properties of Gray Iron-R. G. McElwee, Vanadium Corporation of America, Detroit, and R. Schneidewind, University of Michigan, Ann Arbor.

The Relaxation of Cast Iron-V. T. Malcolm and S. Low, Chapman Valve Co., Indian Orchard, Mass.

STEEL SESSION

Friday, May 12, 10:00 A.M.

Presiding-L. H. Hahn, Sivyer Steel Castings Co., Chicago. Co-Chairman-J. K. McBroom, Stainless Foundry & Engineering Co., Milwaukee.

Manganese Recovery in Acid Electric Steel Making-S. F.

Carter, American Cast Iron Pipe Co., Birmingham, Ala.

Metal Composition Tests for the Steel Melter-H. H. Fairfield, H. F. Graham and A. E. McMeekin, Wm. Kennedy & Sons, Ltd., Owen Sound, Ont. Canada.

JOINT GRAY IRON AND MALLEABLE SESSION Friday, May 12, 2:00 P.M.

Presiding-R. G. McElwee, Vanadium Corp. of America, Detroit.

Co-Chairman-V. A. Crosby, Climax Molybdenum Co., Detroit. Dilatometer Studies of Nodular Cast Iron-N. A. Ziegler, W. L. Meinhart and J. R. Goldsmith, Crane Co., Chicago.

Nodular Iron vs. Section Size-G. Vennerholm, H. Bogart and R. Melmoth, Ford Motor Co., Dearborn, Mich.

STEEL SESSION

Friday, May 12, 2:00 P.M.

Presiding-J. Juppenlatz, Lebanon Steel Foundry, Lebanon,

Co-Chairman-C. H. Lorig, Battelle Memorial Institute, Columbus, Ohio.

The Fayalite Reaction in Sand Molds Used for Steel Castings -H. F. Taylor, Massachusetts Institute of Technology, Cambridge, and R. E. Savage, International Nickel Co., New York

Relation of Thermal Gradients to the Soundness of Cast Steel Plates-H. F. Bishop, Naval Research Laboratory, Washington, D. C.

Influence of Core Mixtures on Hot Tears-Progress Report, Steel Research Committee, presented by Committee Chairman, Clyde Wyman, Burnside Steel Foundry Co., Chicago.

JOINT GRAY IRON AND STEEL SESSION Friday, May 12, 4:00 P.M.

Presiding-T. W. Curry, Lynchburg Foundry Co., Lynchburg,

Co-Chairman-H. H. Kessler, Sorbo-Mat Process Engineers, St. Louis, Mo.

Principles of Risering-Colored Slides-C. F. Walton, Case Institute of Technology, Cleveland.

Principles of Gating-W. H. Johnson, W. O. Baker and W. S. Pellini, Naval Research Laboratory, Washington, D. C.

Finger Gating, Part II-Motion Picture, W. H. Johnson and W. O. Baker, Naval Research Laboratory, Washington, D. C.

PRESIDENT'S ANNUAL ADDRESS

E. W. Horlebein*

An OLD QUAKER SAYING goes like this: "What I give away, I take with me."

No words of mine could better describe the intent, the opportunities and effect of Conventions and Exhibits staged by your Society on behalf of the entire castings industry. It is in the very spirit of A.F.S. that as you contribute to the knowledge of others, so will you add greatly to your own fund of information. The Society owes much to all who have, in the spirit of that old Quaker saying, made possible this year's splendid Convention technical program and one of the finest Exhibits ever seen.

Your Society today has a great many activities, over and above the staging of Conventions and Exhibits. It is a book publisher. Witness the 30 odd special publications of A.F.S., plus the annual bound volume of Transactions. These publications are in constant demand throughout the world. The importance of this activity has resulted in our setting up a special committee on publications policy.

We are also in the business of publishing a magazine American Foundryman. Its recognition editorially and by advertisers is a matter of record, and your Directors have plans for its continued growth.

Eight Research Projects Under Way

A.F.S. today is carrying on eight fundamental research projects in several divisions of the castings field, including brass and bronze, gray iron, light metals, steel, malleable, heat transfer, sand, and cupola operation. Reports of these projects were presented at this year's Convention and in American Foundryman. Every effort is made to avoid duplicating the work of others, and extremely capable men are in charge of each project.

Our chapter organization is being constantly encouraged and improved. Today we have 40 regular chapters and nine student chapters, supported largely by substantial refunds from the dues of chapter members.

The activities of these local groups can hardly be overencouraged, since they bring to thousands the kind of information that can be offered only to hundreds at Conventions such as this.

We are actively engaged in educational work—encouraging young men for foundry careers. All of you are familiar with the slogan conceived by A.F.S. in 1943: "The Foundry is a Good Place to Work." This slogan has gone all over the foundry world, and permission to reprint or use has been freely granted. Our work in the field of education is primarily at the secondary school level, for the most part through chapter educational committees. At the college level, the fine work of the Foundry Educational Foundation has been strongly supported and widely recognized.

Cites Work of Technical Committees

The technical affairs of the Society are carried on through some 100 committees—all but a few within our eight major technical divisions. I constantly marvel at the faithful service of the men who thus serve A.F.S. For example, one committee has had 100 per cent attendance at every meeting held for the past three years. Committee service is open to any member willing to attend meetings and contribute to the work of the Society.

In terms of membership and finances, your Society is sound. During the past 12 months there has been some loss in membership, but less than estimated, due largely to prevalent foundry conditions. This loss will be more than regained with 10 per cent more castings business. Your Board has devoted much thought and effort to building substantially larger financial reserves, reducing operating expenses, and developing additional legitimate sources of income. It must be remembered that the Society is a non-profit corporation, and that it has but one objective in its expenditures: to advance the art and science of metal casting.

A word about two Convention matters. We have been constantly striving for quality in Convention papers . . . every paper and report presented at the 1950 Convention was approved by a Program and Papers Committee before being accepted. The National Office succeeded in preprinting fully 60 papers—

^{*} President. Gibson & Kirk Co., Baltimere, Md.

Delivered during the Annual Business Meeting of the Society at the 1950 A.F.S. Foundry Congress & Show, May 10, 1950, in Cleveland

a new record-for distribution in advance of the Convention either in pamphlet form or in AMERICAN FOUNDRYMAN. These preprints were offered gratis to members on request.

Two exchange papers-one British, one Frenchwere offered for presentation at the 1950 A.F.S. Convention. The British paper, reviewing English achievements in light metal alloys, was presented by Frank Hudson, Mond Nickel Co., Ltd., London, England. The French paper, "Formation of Technical Management Groups for the French Foundries," is by Jean Maurice Laine, head of the French Technical College and vice-president of the French Foundry Technical Association.

This paper describes the establishment in France of a foundry school at the college level. It is government sponsored and financed. A certain number of students is selected for entry each year, and all graduates are definitely placed in foundries. It is stated that 70 per cent enter into executive or supervisory jobs. Just in passing, may I add that England also has a National Foundry College.

Board Formulates Long-Range Policies

Your Board is actively engaged in a long-needed task: correlating previous Board actions and decisions in order to formulate, for the guidance of all, more definite Board and Society policies. It is a big task, but under way.

And now, as I refer once more to our Board, it becomes my sad duty also to report the loss of three of our National Directors since the 1949 Convention in St. Louis. Last June, Director Harry G. Lamker of Teterboro, N. J., was taken; in July, Director Joseph E. Kolb of Peoria, Ill.; and in January this year, Director A. M. Fulton of St. Paul, Minn. The Society can ill afford the loss of men like these, all of whom were long identified with A.F.S. and the castings industry.

During the past two years I have had considerable contact with chapter problems and operations. Last year, while our then-President W. B. Wallis was setting a new endurance record of visiting every chapter, I received many reports of chapter visits by Board members. This year I have visited over a dozen local groups myself, and the Board as a whole has again contacted practically every Chapter of A.F.S. This remarkable evidence of interest by your elected Directors should not go unnoticed, for every one of them takes his responsibilities seriously. For example, our California Director, Robert Gregg, made two 1800-mile trips up and down the Pacific Coast on chapter visits.

Such extensive travel activity may, perhaps explain why, among the officials of A.F.S., weariness and lonely wives are rapidly becoming occupational hazards.

A.F.S. Is "Big Business"

In reading the annual address of a President of some 20 years ago, I noted with astonishment his glow of pride over having experienced during his term of office what he called "the old spirit of close fellowship and camaraderie."

In an organization of nearly 10,000 members, we may yearn for the good old days, but I fear that they, like the days of no income tax, are gone forever. How much gay camaraderie can you generate at the spout of a continuous tap cupola or in a mechanized cleaning room? How much close fellowship can you derive from such a five-ring circus as the 1950 Convention?

Your Society today is not the Society of 1922, the "good old days" when the membership stood at 2300. Today A.F.S. is big business, with an income this year expected to exceed half a million dollars. Such growth has called for self-analysis . . . new perspectives . . . careful examination of our operating pattern . . . lest we dissipate the industrial confidence that has made such growth possible. At the same time, we must never forget nor minimize the importance of the personal element.

Measures to Insure Financial Stability

During the past two years A.F.S. members have noted the effects of a definite policy toward widening the gap between income and expense of certain Society activities. Some of these effects you have approved; others, not. I refer to such items as Convention registration fees, membership dues, advertising solicitation, exhibit space rental fees, and the pricing of some special publications.

The thinking back of these measures is that A.F.S. must be operated more as a business and less as a charitable institution. Every action taken by the Board of Directors is considered in the light of accumulating funds that may be made available for essential activities. We have no so-called "fringe" activities. The consequences of our not having adopted such policies

earlier are now evident to many of us.

For example, the Board recently approved re-establishment of a strong Safety and Hygiene project to develop codes of recommended practice on such subjects as dust control and air pollution. This work will require \$35,000 a year for a minimum 10-year program, and these funds cannot be obtained from current operating revenues of the Society. Therefore, we must look to those firms that realize the need and stand to benefit most from the program for its financial support.

Traditionally-or perhaps I should say notoriously -A.F.S. has failed to blow its own horn sufficiently to acquaint foundry management with its activities and accomplishments. The truth of the matter is that, while management's personal interests undoubtedly have changed in the past quarter century, managing interests of management have not. No foundry operator can afford to ignore the benefits to be derived by his company from active support of A.F.S.

Men like myself 20 years ago were acquiring a personal knowledge of how to make castings through A.F.S. committee service, at A.F.S. Conventions and Exhibits, in A.F.S. chapter meetings, from A.F.S. publications, and through personal contacts with other members. To managers who have gone "up the ladder" and today find other interests, let me say:

Your men are like you were 20 years ago. They are your future executives, your anchors of society-and they are willing to learn as you did; if you encourage them to do so. You can spend your firms' money-and little enough it is-in no wiser way than to insist that they be members and participate to the fullest in what this Society has to offer for their personal advancement.

Training of "New Blood" For Foundries

During the past four years, foundrymen have voluntarily contributed nearly \$600,000 to train and induce young engineers to enter the foundry industry. That is splendid. It marks a new era in progressive foundry thinking. But keep in mind your own wheat when you help the neighbor harvest his. Don't overlook the undeveloped talents and capabilities of your own employees who, like the young engineers, must be encouraged to advance.

Right here I should like once more to refer to the annual address of a former A.F.S. President. Here are President Washburn's remarks in 1939 on manage-

ment's responsibility in A.F.S.:

"The tremendous developments in the recent past and in the present in engineering, in metallurgy and in new materials present a more serious challenge to our industry than it has ever faced, and the successful answer to this challenge cannot be found in the efforts of a few. It calls for the very best that can be brought forward by every firm and individual interested in the casting of metals. Membership is not the responsibility of your officers, not of a committee—it is the responsibility of every member."

I have a strong belief that the foundry industry has not yet assumed its full responsibilities in another direction also . . . the development of a strong and continuous program of castings promotion. This industry has now grown up, and most of our old jealousies and suspicions have been broken down. Through the National Castings Council, for instance, we have developed for the first time a Foundry Advisory Committee to the Munitions Board. Through the Foundry Educational Foundation we have learned how we can advance our mutual interests. Let us now direct our efforts into even broader channels.

When will we realize that the engineers who design metal structures must be impressed, thoroughly and continuously, with the electrifying fact that the shortest distance between raw material and finished product is a CASTING? When will we as an industry learn to spend our dollars in stressing those inherent advantages that have made a casting the most useful and utilitarian of all metal structures? When will we employ our brains and our means to teach others that a casting is both a material and a process . . . and why it is so tremendously important to the engineer?

Recently your Society proposed that A.F.S. and ASME initiate an annual exchange paper arrangement, with papers from ASME on what the engineer seeks from cast metal structures; from A.F.S. on what he can expect from castings. We need this kind of cooperative effort, and we have hopes that our proposal to ASME may be approved . . . but this is only one step forward.

United Effort to Boost Castings

It will require all-out foundry effort to bring the utility of all cast products to the attention of engineers. It is too big a job for any one firm or organization, or for any one segment of the industry. It is a job for all, if we are to avoid duplication of effort.

Nothing will so effectively and permanently refute the charge that the foundry industry is "backward" as will such a joint endeavor. But someone must start the ball rolling. I propose, therefore, to those of you who represent the various foundry organizations, the formation of a Joint Castings Promotion Committee to explore the need and possibilities, to develop a long-term plan and budget, and to report findings and recommendations to the 1951 Convention.

Impressions of a President

As President, I confess to having experienced, many times during the past year, the humility of one whose success depends largely on the work of others. I suppose every new President has certain strong ideas on what he feels is important to accomplish. During the year, these ideas are tempered, beaten, and shaped by discussions with many men, and in the end, the good metal survives and is put to use, the inferior or off-true goes back for recast.

Certain impressions and deep satisfactions remain, however. First, the knowledge of having tried to leave something better than before. Second, the constant warmth of friendliness expressed by the chapters, the Directors, the committee members, and the National Office staff. I cannot express too strongly my respect for the thousands of men who give their time, energy, and brains to A.F.S. so willingly. To the companies who permit their men to give such effort, the Society owes a debt of gratitude.

Lastly, let me leave with you these thoughts: No President of A.F.S. can serve out his term of office without acquiring new respect for all who serve the Society so faithfully. No President can step down without having been broadened in the process and lifted up by the support he has received.

REPORT OF THE SECRETARY-TREASURER

THIS REPORT is intended to sum up the nine major Society activities, with a few comments on problems ahead. The report is confined largely to non-financial matters, finances being discussed in a separate Treasurer's report.

Membership

During 1949-50, Membership in A.F.S. dropped to 9046, a loss of 1017 Members or approximately 10 per cent from the total of 10,063 on June 30, 1949. Budgeted loss was 12.5 per cent. The loss may be charged largely to business conditions in the industry, and partly to the final effects of the dues increases made effective January 1, 1949.

Pro-rationing to Chapter expiration dates has now been completed for the existing membership, and the bulk of Chapter members (91.8 per cent of the total) now are being billed at the same time each year. However, pro-rationing will never be 100 per cent accomplished, due to acceptance of new members at any time throughout the year.

We are glad to report that the time for processing memberships in the National Office now is 7 days, from the time the correct dues payment is received until the membership card is mailed. The 7-day limit has been in effect for several months—and now we are reducing the time to 5 days. We will appreciate the Directors stressing this to the Chapters.

The following table illustrates the fact that a downward trend is with us, and this trend has been recognized by the Finance Committee in recommending a budget of Income for 1950-51:

| | 1311-10 | 1310-13 | 1313-30 |
|------------------------------|---------|---------|---------|
| Members Gained (all Classes) | 1,979 | 1,755 | 1,787 |
| Members Lost (all causes) | . 1,259 | 2,095 | 2,804 |
| Net Gain (or Loss) | 720 | (340) | (1,017) |
| Avge Gain (Loss) per month | | (28 3) | (84 7) |

1947-48 1948-49 1949-50

Dues of Students who graduate now are being invoiced at the higher Personal member rates only when their memberships expire. Formerly they were billed at higher rates immediately on graduation. The change was made at the instigation of several Chapters, in order to give the young men a chance to get their feet on the ground in new jobs.

Total as of June 30......10,403 10,063

Exhibits

A.F.S. returned to Cleveland in 1950 for its eighth Convention and Exhibit in that city. The successful show that resulted had less Exhibitors and less Total Attendance than in 1946 or 1948, but greater Revenue and less Expense. The 1950 Show thus was the most profitable Exhibit in A.F.S. history, as the following figures show:

| | 1946 | 1948 | 1950 |
|-------------------------------|--------|-----------|-----------|
| Total Exhibitors | 258 | 251 | 245 |
| Square Feet Sold 1 | 00,700 | 82,168 | 77,300 |
| Average Sq. Ft. per Exhibitor | 400 | 340 | 322 |
| Rate per Sq. Ft | \$1.50 | \$2.50 | \$2.50 |
| Revenue-Space Rentals\$1 | 42,260 | \$205,420 | \$191,523 |
| Revenue-Permit Fees | 6,875 | 6,600 | 6,325 |
| Total Revenue (Gross) \$1 | 49,135 | \$212,000 | \$197,848 |
| Total Exhibit Expense | 48,145 | 44,840 | 30,096 |
| Net Exhibit Revenue\$1 | 00.990 | \$167,160 | \$167.759 |

Certain problems have appeared in the staging of Exhibits which the Staff is endeavoring to study and solve:

(1) It has become increasingly difficult to reconcile large scale promotion for heavy attendance when the facilities for handling the attendance are limited. This is of little concern to the Exhibitors, who necessarily must be housed properly or we cannot expect them to exhibit. It is, however, a constant reflection on the Society and a considerable loss of time when the Society has to actually enter the hotel business to provide accommodations for its members. Those cities with the best Exhibit facilities lack in housing or other services, and those cities with adequate housing lack in Exhibit and other facilities. This problem was not foreseen when the biennial Exhibit program (which we believe quite adequate for this industry) was initiated.

(2) There is an evident tendency on the part of several groups of Exhibitors to refrain jointly from exhibiting at Foundry Shows, due in part to "competitive" shows and in part to restricted Exhibit budgets. There has been a gradual but sure tendency on the part of many Exhibitors to broaden their markets, which has brought them in contact with other shows, sometimes leaving less of the sales-advertising budget for Foundry Shows.

(3) There is a real tendency for Exhibitors to examine more closely, as in advertising, the quality of attendance and buying habits, as opposed to mere total attendance. This means more promotional work and more selling in the future.

(4) The question of Registration Fees caused some confusion and criticism this year, and deserves further consideration when fees are established for the next Show.

American Foundryman

A report on American Foundryman for the past year naturally must emphasize the steps taken by the Board toward increasing the magazine's revenue from Advertising. Following direction by the Board last January, publisher representatives were interviewed and retained on a commission basis. Their efforts to date have been largely educational, because advertising carried in any one calendar year must be sold in the first 3 months of the same year and the last 3 months of the previous year.

We have come far with AMERICAN FOUNDRYMAN in five years, and it now is definitely accepted for its integrity and content, even though some few still may claim that A.F.S. is "commercializing" by carrying paid advertising. Looked at dispassionately, it is a fine magazine today and one in which we feel justly proud. If the Directors share this feeling, we trust you may seize any and all occasions to say so, for fullest Board support is essential to continued progress.

Certain policies govern the magazine today, for lack of policies established by the Board:

Advertising is sold only on its merits, and no pressure of any kind is exercised under penalty of discrimination in other activities.

No attempt is made to claim greater readership than is known and verifiable.

No special rates or rebates are permitted.

Arrangement of editorial content is primarily for reader convenience.

Editorials are prepared by men of industry, not by Staff members alone.

Transactions material is not duplicated in the magazine in order to save production expense.

A.F.S. does not pay for any article published.

Chapter Operations

No new regular Chapters were added during 1949-50, but two new Student Chapters were approved—at Michigan State College, and the University of Alabama. As of June 30, 1950, A.F.S. had 40 regular and 9 Student Chapters.

The effect of dues increases on the Chapters, reported a year ago, has now diminished, although practically all Chapters lost members during the past year. Only three Chapters showed membership gains: Northern Illinois-Southern Wisconsin gained 8 members; St. Louis District gained 2; Western Michigan gained 17

The 7th Annual Chapter Officers Conference was held June

26-27, 1950, total attendance 90, including 75 Chapter delegates. Chairman Walton L. Woody presided and a total of 12 National Officers and Directors were present for the first time, as recommended by the Board Policy Steering Committee. The Conference, according to reports of delegates, again was highly successful, and the presence of the National Directors fully approved by the Chapter men. This year more broad problems were discussed and fewer critical details brought up, indicating that the Conference probably is "growing up."

Chapter visits by National Directors were continued under Vice-President Woody, and all Directors gave considerable time and effort to establish and maintain closer Chapter contacts. Visits by President Horlebein and Vice-President Woody were of great help, particularly at the Regional Conferences held. Staff talks by the Technical Director, reporter visits by the Editor of American Foundryman and visits by the Secretary all added to better Chapter relationships. Several Chapters accepted the Staff suggestion of purchasing complete A.F.S. libraries at 25 per cent discount for donation to local schools and colleges.

Publications

The Publications Committee continues to do an able and conscientious job in planning and reviewing publication needs and policies, working closely with the Staff. New reproduction equipment purchased during 1949-50 has enabled the production of several publications at very low cost. Full use of this equipment is being made for such publications as the Apprentice Course Outline, Foreman Training Guide, College Research Projects, Foundry Terminology, etc. It is expected that the equipment will be paid for in savings by the end of this year.

The more or less permanent pricing policy for Transactions-\$6.00 pre-publication and \$8.00 post-publication, to Membershas now been generally accepted and successful in disposing of each edition and retaining a minimum supply for future needs.

Convention

The 1950 Convention was attended by 11,588, including 6,032 free (2,054 Exhibitors, 3,052 Northeastern Ohio Day admissions, 440 Guests). An exceptional technical program was provided, and a surprising number of Foreign visitors appeared, including Mr. N. F. Newman, the President of I.B.F.; Mr. John Sissener, Chairman of the Norwegian Technical Foundry Society; and two E.C.A.-sponsored foundry productivity teams (British and Norwegian).

Attendance was smaller than in 1946 and 1948, possibly due to the rail strike that began on Wednesday of the convention week. We sincerely trust that some day Labor will decide to transfer its affections for Spring strike dates to the Fall months, thereby allowing us to again stage an Exhibit year Convention without holding our breath for weeks in advance.

A list of Registered Attendance is available. We believe the Wednesday Banquet was widely popular; that the Chapter Officers Dinner can be dispensed with, especially in Exhibit years; and that the Annual Lecture plus the Annual Business Meeting in one afternoon or morning makes too long a session.

General Administration

The Society occupied new quarters at 616 S. Michigan Ave. on March 3, after 23 years in our old location, and thereby reduced its annual Rentals from \$16,400 to \$10,600 a year. All Directors are invited to inspect the premises. Our new lease runs to September 30, 1953, and affords flexibility of space as well as better working conditions. We sacrificed the Library and Conference Room to provide more space for a Stock and Mail Room, and for the Membership Department.

No Staff turnover in Executive positions occurred during 1949-50. No temporary clerical help is being hired this summer, for the first time in five years, due to more competent membership handling and pro-rationing of dues.

Finances

On July 17, following completion of the Annual Audit by Lybrand, Ross Bros. & Montgomery, Chicago, the Treasurer forwarded to members of the Board certain financial data which reported the best financial record for any single year in the history of the Society, revealing an excess of Income over Expense of \$120,473.18. The budgeted excess of Income, it should be

pointed out, was \$92,619.00.

In the previous Exhibit year 1947-48, excess Income totaled approximately \$42,000. It is significant to note that total Expense for 1947-48 was \$474,335 against \$455,683 in 1949-50. Hence the difference in excess income between these two Exhibit years (approximately \$78,000) was due mainly to increased income as the result of increased dues and other service fees.

The effect of increased service fees shows up also in a comparison of Income and Expense per member in these two Exhibit

| 1947-48 | 1949-50 |
|------------------------------------|-----------|
| Total Membership on June 30 10,403 | 9,046 |
| Total Income\$516,668 | \$578,785 |
| Income per Member \$49.66 | \$63.98 |
| Total Expense\$474,385 | \$455,683 |
| Expense per Member \$45.59 | \$50.37 |

The financial position of the Society on June 30, 1950, was approximately the same as on June 30, 1946, at the close of the 50th Anniversary Convention and Exhibit. In 1946 the Net Worth (Fund Principals) of the Society totaled \$328,197; at the close of 1949-50, \$320,701.

Comparative Condensed Balance Sheets for the past four years (appended elsewhere) show a total of Cash and Investments as follows:

June 30, 1947—\$240,165 " 1948— 285,199 " 1949— 219,590 " 1950— 348,302

Revision of Accounting Methods—During 1949-50 the Society's accounting methods underwent several major revisions to bring them in line with generally accepted accounting practice. One revision was to set up our accounting on an Accrual rather than a Cash basis; second, provision for an Operational form of financial statement; third, consolidation of numerous checking and savings accounts into one checking account (except for Award and Retirement funds).

Canadian Bank Accounts—During 1949-50, three separate bank accounts were set up in Canada to enable the members of our three Canadian Chapters to pay their dues in Canadian funds. It was established at the time that such funds could be withdrawn annually at an exchange rate of only 1/2 of 1 per cent. Devaluation last Fall, however, now means that the exchange rate is 101/2 per cent. It is suggested that consideration be given to a regular withdrawal of Canadian-banked funds for investing in Canada, in Canadian securities. While we will lose on dividends at current rates of exchange, we may eventually regain some of what we now stand to lose.

An alternative would be to discontinue the Canadian bank accounts, take our loss on withdrawals, and again require that Canadian members pay dues in U. S. Dollars. We definitely do not recommend this procedure, because of its probably drastic effect on the memberships of our Canadian Chapters.

Reinvestment of Surplus Funds—The Finance Committee has recommended that surplus Society funds be invested in other than U. S. Government Securities. Since 1942 it has been a Board requirement that only U. S. Securities be purchased. Accordingly, the Harris Trust & Savings Bank of Chicago has been contacted and we now are prepared to turn over \$50,000 of our present \$95,600 in Securities to the bank's Investment Agent Service for reinvestment by them at the rate of 1/2 of 1 per cent. Commercial bonds and non-speculative stocks will be utilized.

Two-Year Budget—Still to be resolved is the oft-discussed question of whether or not the Society should operate on a 2-year rather than an annual Budget. The 2-year Budget has been recommended by the Publications Committee and by the Society's official auditors, and is here further recommended by your Treasurer. Since Exhibit revenues represent 20-25 per cent of A.F.S. Income in any 2-year period, it is suggested that a 2-year Budget, set up at the beginning of an Exhibit year, should enable better planning so as to avoid a tendency toward "feast-and-famine" operations in alternate years. Such a Budget, of course, can and should be reviewed annually for possible revisions. It is suggested that the question be submitted to the Board Policy Committee on Finances for study and recommendations.

The Society's finances are now in good shape, following a highly successful Exhibit year. Expenses are under close and constant control of the Finance Committee and Treasurer, and all Staff members responsible for expenditures are kept continuously cost-conscious, in keeping with the requirements of their activities.

Recognition

To President Horlebein, for his untiring patience and sincerity of purpose, as well as his constant willingness to assist the

National Office Staff through an active year, we offer our deepest respect. To the Officers and Directors who have worked closely with us during the past 12 months toward the solution of many perplexing problems, we extend appreciation for counsel and guidance. It has been an eventful year of problems—and of progress, too—made successful and satisfying by the sincere cooperation of the membership and the Board.

Respectfully submitted WM. W. MALONEY Secretary-Treasurer

Condensed Statement of Income and Expense

(All Expenses Distributed to Major Activities)
Fiscal Year July 1, 1949—June 30, 1950

| INCOME | | |
|----------------------------|--------------|----------|
| | Total | Income |
| | Amount | Per Cen |
| Membership Dues | \$182,780.56 | 31.5 |
| General Publications | | 1.6 |
| Special Publications | 24,231.50 | 4.2 |
| AMERICAN FOUNDRYMAN | | 24.3 |
| Convention & Exhibit 1950 | 222,211.89 | 38.4 |
| TOTAL INCOME | \$579,936.86 | 100.0 |
| EXPENSE | | |
| | Total I | Expense |
| | Amount | Per Cent |
| Membership Department | \$ 25,840.48 | 5.6 |
| Research | 32,625.00 | 7.1 |
| Other Technical Activities | 21,607.10 | 4.7 |
| General Publications | 21,835.79 | 4.8 |
| Special Publications | 25,146.60 | 5.5 |
| AMERICAN FOUNDRYMAN | 145,194.20 | 31.6 |
| Chapter Operations | 73,356.49 | 16.0 |
| Convention & Exhibit 1950 | 75,053.96 | 16.3 |
| General Administration | 24,431.16 | 5.3 |
| Retirement Plan | 14,372.90 | 3.1 |
| TOTAL EXPENSE | \$459,463.68 | 100.0 |
| Excess Income over Expense | 120,473.18 | |
| TOTAL | \$579,936.86 | |

Comparative Condensed Balance Sheets

As of June 30, 1947-48-49-50

| | As of June 30, | 1747-40-47-50 | | |
|-----------------------------------|----------------|---------------|---------------|---------------|
| Assets | June 30, 1947 | June 30, 1948 | June 30, 1949 | June 30, 1950 |
| Cash | \$ 58,216.09 | \$106,394.93 | \$ 85,785.57 | \$229,497.13 |
| Accounts Receivable | 9.457.28 | 10,003.03 | 4,405.03 | 22,011.00 |
| nventories | 23,898.63 | 33,167.40 | 35,567.65 | 35,354.99 |
| nvestment Securities | 181,949.60 | 178,805.60 | 133,805.60 | 118,805.60 |
| Deferred & Prepaid Items | 13.815.04 | 19,721.40 | 19,553.00 | 31,530.03 |
| furniture & Fixtures (net) | 6,354.21 | 10,744.57 | 9,791.42 | 16,451.18 |
| TOTAL | \$293,690.85 | \$358,836.93 | \$228,908.27 | \$453,649.93 |
| LIABILITIES | + | | - | |
| ccounts Payable | \$ 26,450.98 | \$ 20,899.06 | \$ 17,085.31 | \$ 24,665.99 |
| ccrued Items | 667.74 | 1,099.19 | 470.91 | 148.34 |
| ocial Security Payroll Deductions | 233.31 | 276.99 | 204.27 | 53.12 |
| Deferred Dues Income | 24,214.13 | 61,439.16 | 71,217.06 | 108,081.29 |
| und Principals | 242,074.69 | 275,122.53 | 199,930.72 | 320,701.19 |
| TOTAL | \$293,690.85 | \$358,836.93 | \$288,908.27 | \$453,649.93 |
| (Note: All award funds included.) | * | | | |

REPORT OF THE TECHNICAL DIRECTOR

THIS PROCRESS REPORT is presented to cover the year beginning July 1, 1949 and ending June 30, 1950.

Technical Committees

In conformity with what we consider sound policy, a number of changes have been made in committee personnel during the past year, necessitated by inactivity on the part of certain individuals, inability to devote sufficient time to advantageously execute the responsibilities with which the Committee has been charged, or because the Committee has been terminated due to the fact that its assigned task has been completed. Such a policy, it is felt, leads to energetic activity on the part of all committee personnel, for they realize that unless satisfactory performance is evidenced, they will of necessity relinquish the honor of holding such office. As a result, committee work becomes much more effective.

During the past year several new committees have been activated to undertake specific problems, as follows:

(A) Essay Contest Committee

(B) Foundry Terminology Committee

(C) Cast Metals Handbook Revision Committee

The new edition of the National Committee Personnel Roster has now been sent to all committee members, as well as members of the Board of Directors and Sustaining members of the Society. The production cost of this Roster was approximately 50 per cent less than two years ago, these savings being made possible by utilizing the same format and taking advantage of considerable of the type and engravings still standing.

AFS-Sponsored Research

Results obtained from research projects sponsored by the Society are gratifying. During the past year the Malleable Research was completed, and the other projects are progressing in a very satisfactory manner. At reasonably regular intervals and especially when pertinent matters require consideration, each of the research committees has held meetings throughout the year with representatives of the contractors. A summary of each of the current projects is as follows:

Aluminum and Magnesium Research Project (Battelle Memorial Institute, Columbus, Ohio). This project was initiated in the Fall of 1947 as the first Society-sponsored Research Project under the new plan. It has as its purpose a fundamental study of the hydraulics of flow of metal into a mold as influenced by the shape and dimension of the entire gating system. Because direct visual observation is possible, this work has largely been confined to the utilization of water as a working fluid when poured into Lucite or transparent plastic molds of different shapes. As the water enters the mold, high-speed motion pictures have been taken to record the conditions within the mold at various important points. More recent work with test castings made of aluminum cast in sand confirmed the results obtained by using water in the transparent mold.

The first formal progress report was presented by the 1949 Annual Meeting of the Society in St. Louis, accompanied by a motion picture film portraying the actual recorded experimental results. Three copies of the film were made and have been shown this year to over 75 Chapters, educational institutions and company employee groups. In order to defray the cost of repairing copies of the film that are damaged or worn through normal usage, a fee of \$10 was charged for each showing, with the exception of educational institutions and other societies with which we maintain close liaison. Thus the revenue from these showings will defray a large portion of the production cost of the film.

The Technical Director has personally served as speaker and presented this film before approximately 15 groups. In other instances a member of the Aluminum and Magnesium Division Research Committee accompanied the film to insure that the

audience would have a well qualified presentation. Many personal letters have been received expressing appreciation for the film.

A second comprehensive research report and a new film were presented at the 1950 Annual Meeting, in Cleveland. The new film will be a color-sound motion picture and includes a complete record of the present status of this research. Because of the increased production cost for a color-sound film, a charge of \$20 per showing will be made. It is anticipated that it will meet with even greater approval because of the advantages of the sound film and better portrayal of what is actually happening in the mold when colored photography is employed.

Upon request of the Committee, the Board of Directors has

Upon request of the Committee, the Board of Directors has approved the expenditure of sufficient funds to produce the color-sound film. In view of its basic nature and interest to all branches of the Foundry Industry, it is strongly recommended that authority for the extension of this research be granted for another year, by the appropriation of \$6,500.

Brass and Bronze Research Project (University of Michigan, Ann Arbor, Michigan). The brass and bronze foundryman has long needed a rapid, reliable test to indicate melt quality of tin bronzes prior to pouring the metal into the mold. This research has as its primary purpose the development of such a test, and after approximately two years of work, the results indicate that such a test can be developed. Progress reports have been presented at the 1949 and 1950 Annual Meetings of the Society.

Cooperative work is now being solicited from brass and bronze foundrymen by requesting them to actually run tests as developed through this research so that, after utilization of the test method in actual production, it will be learned whether it is wholly satisfactory or requires further refinement. In the interim, the research is continuing to further extend the usefulness of the test and decrease the time required for its completion. In other words, this work has developed a test which now is in a state where it appears to require only the necessary refinements for its successful adoption.

The Research Committee has requested a fiscal appropriation of \$5,000 to carry on the work for another year and it is recommended that the Board grant this request.

Centrifugal Casting Research Project (Bureau of Mines, Ottawa, Ontario, Canada). Approximately two years ago this project was initiated cooperatively with the Canadian Bureau of Mines as a study of the centrifugal casting of light alloys. The Society provided a total of \$1,000 which was used to purchase necessary materials for conducting the research, all labor and facilities being provided by the Bureau.

A Progress Report of this work was presented at the 1949 Convention and the studies are continuing. So far as we now know, no additional funds will be required from the Society. Further work is being conducted at the Bureau of Mines with the guidance of the Centrifugal Casting Committee, and it is anticipated that another progress report will be available for presentation at the 1951 Annual Meeting.

Cupola Research Project. This research has been financed from a special Cupola Research Fund. The initial effort of the Committee was the preparation of a complete bibliography of all published material relating to the cupola. As a second step, it undertook the production of the Handbook of Cupola Operation, 5,000 copies of which were produced at the first printing and completely sold about a year ago, necessitating the reprinting of the first edition. The Committee now is preparing a revised edition, which probably will not be available in its final form for approximately six to eight months.

Under the Chairmanship of Richard G. McElwee, whose untimely death occurred on June 21 this year, the Committee undertook a number of projects of basic interest to cupola operators, and these have been published in various issues of AMERI-

CAN FOUNDRYMAN so as to make them available to the entire membership. These are as follows:

- 1 Cost Reducing Melting Suggestions.
- 2. Cupola Operation with Heated Blast.
- 3. Coke Boosters a Fallacy in Cupola Operation?
 4. Slag Control is Important in Cupola Operation.
- Maintaining Carbon-Silicon Ranges in Cupola Melting.
- 6. Enriching Cupola Blast with Oxygen Additions.
- 7. Foundry Records Aid Control.
- 8. The Problem of Air Pollution.
- 9. Basic Equipment for Foundry Chemical Laboratories.
- Metallographic and Physical Testing Equipment for Foundry Laboratories.
- 11. Auxiliary Oxygen Applied in 72 Inch Production Cupolas.
- 12. Establish Foundry Controls Through Laboratory Services.

Because of the large volume of work involved, a full time research metallurgist, under the direction of the Committee, has been immediately responsible for execution of the various projects outlined. This research worker, Theodore Giszczak, has been in the employ of the Society and paid from Cupola Research Funds, since August 1, 1947. Through the courtesy of Mr. McElwee and the Vanadium Corp. of America, he has had desk space in the offices of that organization in Detroit, making it convenient for the Chairman of the Committee to maintain close contact with the research activities.

STUDY OF BEHAVIOR OF COKE

Much work was done on the study of the behavior of coke in the cupola, through the generosity of the Ford Motor Co., who supplied coke made under varying coking conditions. Unfortunately, because of conditions beyond the control of the Committee, the tests were not conclusive and since then, due to the coal strike and other circumstances which arose, additional coke has not been available to continue this particular investigation.

As part of this project, the Committee has been cooperating with the Harry W. Dietert Co. and the Canadian Bureau of Mines in an endeavor to produce a satisfactory laboratory testing technique which may make it possible to pre-evaluate coke prior to its actual utilization in the cupola. Briefly, this test involves the measurement of the electrical resistance of a sample of coke after it has been crushed and screened to a uniform grain size and compressed under a given hydraulic pressure. The equipment has been developed and it appears now that it should be placed in the hands of a foundry having adequate interest and qualified personnel to use it in conjunction with actual foundry production, to accumulate a large amount of data which may subsequently be statistically evaluated in order to determine what the test is capable of predicting, and within what limits of accuracy.

SLAG ANALYSIS INVESTIGATION

The Committee has also investigated the possibility of arriving at an acceptable slag analysis utilizing methods developed by the Bethlehem Steel Co., namely, crushing the slag to a fine powder and dissolving a weighed amount in a specific volume of water, and finally measuring its pH value. It was hoped that this would provide a quick, economical method of analyzing cupola slags and thereby make it practical for the foundryman to carefully centrol slag composition, which is known to have a pronounced influence on cupola behavior and certainly refractory life. This activity has been conducted cooperatively with Wayne University at Detroit and still requires considerable additional work. It is believed that Wayne University will be glad to continue this work and, at some future date when sufficient and useful data has been acquired, offer this information in the form of a technical paper.

In conjunction with its other studies, the Committee has also made a survey of some of the problems relating to air pollution, as produced by the effluent gases emanating from the cupola. It is quite likely that some of this information will be of value to the Safety and Hygiene program when it gets under way.

More recently, the Committee has cooperated with the Gray Iron Division Chill Test Committee in the development of standard chill tests suitable for evaluating the chilling tendency of irons of varying carbon equivalents.

With the loss of Mr. McElwee as Chairman of the Cupola Re-

search Committee, it is very doubtful in the writer's mind that we will be fortunate enough to find another man to serve as Chairman of this Committee who will have both the interest and the time available to profitably direct the efforts of a full-time research metallurgist. As further evidence of this, the Vice-Chairman of the Committee has already informed us that he could not devote the time and effort necessary to serve as Chairman of the Committee. It is apparent, therefore, that these researches will have to brought under much more intimate direction of the Technical Director of the Society.

Based upon the experience we have had with eight other research projects, each under annual contract, it would be the writer's recommendation that the Society continue the Cupola Research in a manner similar to its other research projects. Under such a plan, the Committee can delineate a particular object, invitations to bid can be sent to a number of qualified research laboratories, and after selecting the successful bidder, a definite contract can be prepared covering a one-year period. At the end of the contract interval, we are in a position to take inventory of the results obtained and make the decision as to whether the research shall be terminated or continued for another one-year period.

This plan provides a much closer control of expenditures and better evaluation of results obtained. If it is the wish of the Cupola Research Committee to actually continue substantial research projects, they can then define their program and use this as the basis for solicitation of additional funds, inasmuch as the present fund will be practically exhausted by the end of this year.

Gray Iron Research Project (Massachusetts Institute of Technology, Cambridge, Mass.). The Gray Iron Research Project was initiated at the above institution approximately six months ago and comprises a study of the gating and risering of gray iron castings so as to achieve solidity at minimum cost. Initial efforts were devoted to preparation of a complete bibliography covering this field, which has been completed and is now available in published form as the first Progress Report of this research project. Since then a second progress report has been prepared and is now published.

The work at present is being conducted cooperatively with the Gray Iron Division Gating and Risering Committee, and through the generosity of several of the foundries represented by members of this Committee, test castings of various shapes are being made from several grades of iron, utilizing different gating and risering techniques. The castings are shipped to M.I.T. for critical analysis and will serve as the basis of further research. This precedure has been followed so as to economize on the expenditure of research funds, knowing that such initial test castings could be made much more economically in a production foundry than under research laboratory conditions.

It is felt that this project is progressing satisfactorily. Upon recommendation of the Committee, it is requested that the Board give consideration to authorizing the expenditure of \$5,000 during this fiscal year for this research.

Heat Transfer Research Project (Columbia University, New York, N. Y.). This project has continued for several years at Columbia University under the guidance of the Heat Transfer Committee. For several years progress reports have been presented at the Annual Conventions and subsequently published in Transference.

Essentially, this work comprises the measurement of heat flow under various conditions existing within a mold, utilizing electrical equipment and supporting these measurements by actual bleeding tests made on sample castings, the latter work being done cooperatively by various foundries. The data acquired is fundamental in nature and it is believed that over a period of time will afford very useful tools for the foundryman.

Since progress has been excellent, the Committee again has requested that the annual sum of \$2,000 be granted to continue this research for another year.

Because of the recognized importance and value of this work, the Institute of British Foundrymen has invited Dr. Victor Paschkis, in charge of our heat transfer research at Columbia, to prepare n paper on heat transfer as it relates to the foundry, for presentation at their Annual Meeting in England.

Malleable Research Project (University of Michigan, Ann Arbor, Mich.). The first malleable research project was completed in the Spring of this year, comprising a study of the most desirable microstructure in pearlitic malleable iron, preparatory to either flame or inductive hardening of this material. After two years of work, a final report was prepared and presented at the 1950 Annual Meeting of the Society in Cleveland.

The Malleable Research Committee has since decided that the next research project should be a basic study of the effect of melting conditions, and particularly furnace atmosphere, on the subsequent behavior of malleable iron, in terms of annealability, development of hot cracks, fluidity, etc. Invitations to bid have been sent to approximately 15 research institutions and we are now awaiting bid proposals. When all replies have been received, the Research Committee will meet to select the successful bidder. The writer will then enter negotiations preparatory to execution of a definitive contract for one year's work, with the successful bidder. At the semi-annual meeting of the Board, held in January, an expenditure of \$5,000 was approved for this project.

Sand Research Project (Cornell University, Ithaca, N. Y.). The oldest research project sponsored by the Society is the Sand Research at Cornell University, initiated approximately 11 years ago. At our 1950 Annual Meeting the 10th Annual Progress Report was presented, covering the past year's work, and is published in this volume of Transactions. See p. 133.

In recent years this investigation has confined itself almost exclusively to the high temperature properties of molding sands, and it is believed that sufficient fundamental information has been acquired so that in the coming year experiments will be conducted in the foundry, as well as in the laboratory, in an endeavor to correlate these findings with the foundryman's problems and thereby afford him specific information so as to achieve better control of molding sand behavior.

Because of its basic interest to all branches of the Foundry Industry, and by the request of the Sand Research Committee, it is recommended that a sum of \$5,000 be authorized for another year's extension of this investigation.

Steel Research Project (Armour Research Foundation, Chicago). The Steel Research Project was initiated in March 1948 and has as its purpose a fundamental study of the influence of core sand characteristics on the development of hot tears in steel castings. It is hoped that this research will ultimately yield a suitable test specimen which may be used to evaluate the relation of core properties to the development of hot tears. A considerable fund of information has been accumulated and it appears that we are now entering a period where the results will be productive.

The Committee has asked that \$5,000 again be authorized to continue this research during 1950-51.

Convention Program

Based upon numerous reports, the 1950 Technical Program was considered highly satisfactory. A total of 60 papers were preprinted as separate publications or in pre-convention issues of AMERICAN FOUNDRYMAN. More than 2,800 requests were received from the members of the Society residing in the United States, and in excess of 450 requests from foreign members, making a total of approximately 3,250 individual orders for preprints. In all cases where these requests were received before the specified deadline date, preprints were mailed to the members approximately one month in advance of the Convention, affording them an adequate opportunity to critically review the papers and come prepared to enter into an intelligent discussion following presentation of papers.

Several jointly sponsored sessions were part of this year's technical program, in order to create a greater degree of interest. For the first time, in addition to the Gray Iron and Sand Shop Operation Courses, a Nonferrous Shop Operation Course was scheduled for two evening sessions, organized and sponsored by the Brass and Bronze Division and confined to the melting of copper-base alloys, either by liquid, gaseous or electric fuel. In addition, joint sessions were sponsored by the Malleable and Gray Iron Divisions, the Gray Iron and Steel Divisions, and the Steel and Sand Divisions. The Plant and Plant Equipment Committee was particularly successful this year in sponsoring two sessions before which a total of seven papers were presented, covering various phases of cleaning room problems.

During the course of the Convention, a total of 52 technical functions were offered, at which a total of 94 technical presentations were made.

Publications

The 1949 Transactions (vol. 57) comprising approximately 700 pages, was published and shipped during the month of November 1949, to all who had ordered it on a pre-publication basis. A total of 1,500 clothbound copies were printed, of which nearly a thousand had been sold at the pre-publication price of \$6.00, or distributed as gratis copies to Sustaining and Honorary Members on request, leaving a comparatively small balance to be sold at \$8.00 to members of the Society, or \$15.00 to nonmembers.

Of the books approved by the Publications Committee and the Board of Directors for production during 1949-50, the following were produced and made available for sale:

Foundry Core Practice (revised)—Available in February 1950. Guide for Foreman Training Conferences—Available in January 1950.

Foundry Research Projects—Being distributed gratis to a selected list of educators. Limited number furnished gratis to Foundry Educational Foundation for distribution to recognized schools. Purpose of this publication is to interest graduate college students in foundry research and accomplish the dual purpose of supplying additional information to the foundry, as well as to make the student foundry conscious.

Bibliography of Centrifugal Casting.
Foundry Apprentice Course Outline—Prepared by the Educational Division Apprentice Training Committee as a companion to Apprentice Training Standards for the Foundry Industry, published about a year ago, thereby providing

complete coverage on the subject.

Gray Iron Progress Report No. I—Copies were sent gratis to all Sustaining Members of the Society, as evidence of additional service rendered. As subsequent research progress reports are available, they will be similarly distributed to Sustaining Members gratis.

Foundry Terminology—This glossary of foundry terms has been published in tentative form and, with additions and corrections which we hope to receive from this Committee, it will be published later in final form.

Of a total of 11 books which were planned for production in 1949-50, five have been produced. Due to conditions beyond our control, since completion of manuscripts is the result of committee effort and at times this work does not progress as rapidly as anticipated, the remaining manuscripts were not received in time for publication in the past year. A publication schedule for 1950-51 has been approved by the Publications Committee as required by the Board of Directors in forming the fiscal year's budget.

It is gratifying to report that excellent progress is being made on the *High School Foundry Text*, under the authorship of Edwin W. Doe, Sr. Brocklyn Technical High School, Brooklyn, N. Y., with the guidance of the Text Book Committee of the Educational Division. It is anticipated that this book will be ready for publication some time early this Fall, and undoubtedly will be produced by a textbook publisher under a royalty agreement with the Society.

The College Foundry Text is likewise progressing, under the authorship of Prof. P. E. Kyle of Cornell University, Ithaca, N. Y., and it is now hoped that by the end of the calendar year 1950 we will have the completed manuscript. Similarly, actual production of the College Textbook will be under contract with a school textbook publisher.

The Sand Handbook is being revised by the Handbook Revision Committee of the Sand Division and it now appears that this Committee will have the finished manuscript available some time during 1950-51.

A drastic revision of the Cast Metals Handbook is being planned and for this purpose a Committee is being organized, comprising one member from each of the Technical Divisions of the Society, to establish the style and format of the new edition. After this Committee has been finalized, it is intended to employ the part-ime services of a well qualified man who will serve as editor of the revised edition.

Advance or pre-publication order forms for the Annual Transactions of the Society for the year 1950 (vol. 58) have been sent to the entire membership. Subscriptions to these Transactions will again be on a pre-paid, pre-publication basis at a price of \$6.00 to members and \$15.00 to non-members, with

gratis copies (on request) to all Sustaining and Honorary Members. It is anticipated that Transactions will be available for shipment about the middle of November 1950. Subsequent to the date of publication, members will be charged \$8.00 per copy and non-members \$15.00 for the same publication. Only a cloth-bound edition will be produced. (Prices and publication policy determined by the Publications Committee).

Technical Inquiries

Technical inquiries, even though they may appear to be relatively inconsequential and comprise but a small part of the total effort expended on the technical affairs of the Society, are received each year in substantial numbers. Up to the present time, a total of 582 individual requests for technical information and assistance have been answered, in many instances involving a detailed analysis of the information presented and the transmittal of specific remedial recommendations. Lack of a library and a qualified technical librarian makes these replies particularly difficult and time consuming and, in some instances, not nearly as complete as desirable because of the lack of sufficient time to thoroughly investigate all pertinent references.

Apprenticeship Contest

As it has since 1924, the Society again sponsored its Annual Apprentice Contests. Prior to this year, the contest was open only to light metals, copper-base, gray iron, steel molding and wood patternmaking. For the first time, in accordance with action of the Board of Directors, the contest was extended to include metal patternmaking, which met with unusual success, based upon the number of entries in the latter field.

A total of 267 contestants from 98 companies and 10 Chapters participated in the contest. These numbers are in excellent agreement with previous years' contests and should provide ample basis for a continuation of the activity. The Apprentice Contest Committee of the Educational Division deserves a great deal of credit, beginning with the preparation of the contest drawings to the final judging of the contest entries.

Each year it becomes apparent that further elaboration of the contest rules should be made and, before the next year's contest, considerable revision is contemplated and already in progress. It is felt that the Apprentice Contest's exhibit at this year's Convention was among the best, as evidenced by the many favorable remarks received from those who saw it.

Chapter Aids

To assist the Chapters in preparing a good technical program for the year's meetings, a new potential speaker list was prepared and made available to all Chapter representatives who attended the Chapter Officers Conference held in June. It is apparent that the Chapters are making valuable use of this list, judging from the replies from the various Chapter representatives at the Conference when asked to list the two best speakers during the past year. Of those listed, approximately 80 per cent were already included on the Chapter Speaker List prepared by the Technical Staff of National Headquarters.

As an added service to the Chapters, we again have prepared a new and revised list of films known to be available and of probable interest for showing at a Chapter meeting. These lists likewise were made available to all those attending the Chapter Officers Conference.

Technical Cooperation

Close liaison with other technical societies continues, the Technical Director holding memberships in the following:

American Society of Mechanical Engineers
American Society for Metals
American Institute of Mining and Metallurgical
Engineers
American Ordnance Association
American Society for Testing Materials
Society for Non-Destructive Testing
Society of Automotive Engineers
Society for Advancement of Engineering Education

At the Annual Meeting of the ASME, the writer was elected a member of the Executive Committee of the Metals Engineering Division, and subsequently was appointed a member of the Professional Divisions Committee of the same Society. The Technical Director has also been appointed a member of the Technical Advisory Committee of the Munitions Board of the Armed Forces.

Active liaison with these various groups is essential to further our Society, as well as to make them FOUNDRY conscious.

Respectfully submitted, S. C. Massari Technical Director

5-Year Comparative Membership Report

As of June 30, 1946-1950, Inclusive

| | June 30, 1946 | June 30, 1947 | June 30, 1948 | June 30, 1949 | June 30 1950 |
|------------------------------|---------------|---------------|---------------|---------------|--------------|
| Sustaining Members | 183 | 193 | 199 | 221 | 197 |
| Company Members | 1,462 | 1,548 | 1,525 | 1,380 | 1,229 |
| Personal Members | 6.426 | 7.358 | 7,860 | 7,508 | 6,571 |
| Student & Apprentice Members | 20 | 89 | 307 | 430 | 550 |
| Honorary Life Members | 58 | 64 | 63 | 69 | 75 |
| Foreign Members | 390 | 430 | 449 | 455 | 424 |
| | - | - | | | |
| TOTAL | 8,539 | 9,683 | 10,403 | 10,063 | 9,046 |
| New Members, 12 Months | 1.743 | 2,164 | 1,979 | 1,755 | 1,787 |
| Resignations | 370 | 186 | 286 | 303 | 484 |
| Delinquents Dropped | 631 | 829 | 948 | 1,759 | 2,286 |
| Removed by death | 18 | 5 | 25 | 33 | 34 |
| Net Gain (Loss) for Year | 724 | 1,145 | 720 | (340) | (1,017) |
| | | | 0.410 | 0.040 | 0.000 |
| Members in Chapters | 7,767 | 8,809 | 9,519 | 9,253 | 8,306 |

A. F. S. Membership Distribution

June 30, 1950

| Chapter | Sustaining | Company | Personal | Personal (Affiliate) | Personal (Associate) | Student & Apprentice | Honorary Life | Total |
|--|------------|---------|----------|-------------------------|----------------------|----------------------|------------------|-------|
| Sirmingham District | 5 | 27 | 70 | 103 | 4 | 8 | 2 | 219 |
| ritish Columbia | 0 | 14 | 20 | 22 | 7 | 14 | 0 | 77 |
| anton District | 1 | 26 | 23 | 68 | 1 | 0 | 0 | 119 |
| entral Illinois | i | 9 | 25 | 52 | î | 2 | 1 | 91 |
| entral Indiana | 4 | 29 | 49 | 120 | 3 | 3 | 0 | 208 |
| Central Michigan | 0 | 11 | 30 | 53 | 3 | 1 | 0 | 98 |
| Central New York | 1 | 25 | 44 | 58 | 4 | 9 | i | 142 |
| Central Ohio | 6 | 24 | 38 | 103 | 5 | 2 | 3 | 181 |
| | 3 | 21 | 49 | 53 | 29 | 2 | 8 | 165 |
| Chesapeake | 22 | 98 | 178 | 412 | 16 | 20 | 9 | |
| Chicago | | | | | | | | 755 |
| Cincinnati District | 14 | 38 | 75 | 119 | 7 | 22 | 3 | 278 |
| Detroit | 9 | 46 | 117 | 221 | 16 | 18 | 11 | 438 |
| Eastern Canada | 6 | 40 | 63 | 151 | 17 | 11 | 1 | 289 |
| astern New York | 0 | 12 | 16 | 38 | 3 | 9 | 0 | 78 |
| Metropolitan | 9 | 51 | 116 | 132 | 19 | 8 | 4 | 339 |
| Mexico City | 0 | 5 | 20 | 10 | 0 | 3 | 0 | 38 |
| dichiana | 1 | 24 | 63 | 81 | 2 | 1 | 0 | 172 |
| Io-Kan | 1 | 6 | 15 | 11 | 3 | 2 | 0 | 38 |
| Northeastern Ohio | 20 | 68 | 126 | - 258 | 13 | 17 | 7 | 509 |
| Northern California | 6 | 19 | 83 | 76 | 7 | 5 | 0 | 196 |
| No. Illinois-So. Wisconsin | 4 | 16 | 22 | 62 | 1 | 2 | 1 | 108 |
| Northwestern Pennsylvania | 1 | 26 | 35 | 78 | 0 | 1 | 0 | 141 |
| Ontario | 6 | 69 | 87 | 106 | 3 | 5 | 1 | 277 |
| Oregon | 2 | 6 | 26 | 57 | 5 | 2 | Ô | 98 |
| hiladelphia | 15 | 56 | 108 | 153 | 11 | 0 | 5 | 348 |
| Quad City | 4 | 36 | 46 | 99 | 4 | 1 | 2 | 192 |
| | 1 | 10 | 32 | 45 | 2 | 4 | 0 | 94 |
| Rochester | 6 | | | 180 | 5 | | 1 | |
| aginaw Valley | - | 15 | 21 | | | 32 | | 260 |
| t. Louis District | 2 | 37 | 93 | 117 | 6 | 16 | 0 | 271 |
| outhern California | 4 | 42 | 123 | 77 | 11 | 2 | 0 | 259 |
| Tennessee | 0 | 18 | 40 | 58 | 3 | 2 | 0 | 121 |
| exas | 5 | 23 | 52 | 43 | 4 | 1 | 0 | 128 |
| imberline | 1 | 4 | 28 | 17 | 7 | 0 | 0 | 57 |
| Toledo | 1 | 18 | 22 | 42 | 1 | 1 | 1 | 86 |
| Tri-State | 0 | 9 | 39 | 25 | 2 | 0 | 1 | 76 |
| Twin City | 4 | 28 | 45 | 73 | 7 | 0 | 2 | 159 |
| Washington | 0 | 8 | 24 | 9 | 12 | 0 | 0 | 53 |
| Vestern Michigan | 4 | 25 | 32 | 156 | 2 | 2 | 1 | 222 |
| Western New York | 1 | 32 | 63 | 70 | 3 | 0 | 1 | 170 |
| Visconsin | 16 | 77 | 88 | 245 | 12 | 23 | 3 | 464 |
| The control of the co | | | | - | | | | |
| TOTAL REGULAR CHAPTERS STUDENT CHAPTERS | 186 | 1148 | 2246 | 3853 | 261 | 251 | 69 | 8014 |
| fassachusetts Inst. of Technology | bearing. | | - | _ | 1 | 47 | _ | 48 |
| Michigan State College | | | | | | 37 | | 37 |
| dissouri School of Mines | | | | | 3 | 62 | _ | 65 |
| Ohio State University | _ | _ | _ | | 1 | 36 | _ | 37 |
| | _ | _ | _ | _ | | 24 | - | 24 |
| Oregon State College | _ | _ | _ | _ | - | 14 | _ | |
| exas A. & M. College | _ | - | _ | _ | _ | | _ | 14 |
| niversity of Alabama | _ | _ | _ | _ | - | 9 | _ | 9 |
| niversity of Illinois | _ | _ | _ | _ | _ | 44 | _ | 44 |
| niversity of Minnesota | - | | - | - | _ | 14 | _ | 14 |
| Tonia Saurana Communication | | | | _ | = | 997 | | |
| TOTAL STUDENT CHAPTERS | - | | - | 8058 | 5 | 287 | - | 0000 |
| TOTAL ALL CHAPTERS | 186 | 1148 | 2246 | 3853 | 266 | 538 | 69 | 8306 |
| Son-Chapter | 11 | 81 | 100 | 70 | 36 | 7 | 5 | 310 |
| Foreign | 0 | 30 | 381 | 4 | 9 | 5 | 1 | 430 |
| GRAND TOTAL | 197 | 1259 | 2727 | 3927 | 311 | 550 | 75 | 9046 |

Minutes

Special Meeting 1949-1950 Executive Committee

Palmer House, Chicago-July 28, 1949

Present: President E. W. Horlebein, presiding Vice-President W. L. Woody

Directors:

F. C. Riecks J. M. Robb, Jr. W. B. Wallis V. E. Zang

Secretary-Treasurer Wm. W. Maloney

Technical Director S. C. Massari

President Horlebein requested the Secretary to read Art. X Secs. 1-9, inclusive, of the By-Laws covering appointment of a Nominating Committee for the election of National Officers and Directors. The Secretary presented the names of candidates submitted by 22 eligible Chapters, 4 elegible Chapters failing to name candidates.

Following discussion and selection in accordance with the By-Laws, motion was made, duly seconded and carried, appointing the following seven members of the Society to serve with the two immediate past-Presidents of A.F.S. on the 1950 Nominating Committee:

Past-President W. B. Wallis, President, Pittsburgh Lectromelt Furnace Co., Pittsburgh, Pa.—Chairman.

Past-President Max Kuniansky, Vice-President & General Manager, Lynchburg Foundry Co., Lynchburg, Va.

J. A. Durr, Consulting Metallurgist, Albion, Mich.—(Rep. Central Michigan Chapter and Fdry. Services-Malleable Iron).

W. A. Hallberg, Foundry Engineers, Lakey Foundry & Machine Cc., Muskegon, Mich.— (Rep. Western Michigan Chapter and Gray Iron).

Henry Louette, Asst. Superintendent, Warden King Ltd., Montreal, Que., Canada—(Rep. Eastern Canada Chapter and Gray Iron).

B. A. Miller, Supt. of Foundries, Baldwin Locomotive Works, Philadelphia—(Rep. Philadelphia Chapter and Brass & Bronze).
M. W. Pohlman, Vice-President, Pohlman Foundry Co., Buffalo, N. Y.—(Rep. Western New York Chapter and Gray Iron).

 H. S. Simpson, Chairman of Board, National Engineering Co., Chicago—(Rep. Chicago Chapter and Foundry Equipment).
 C. Neal Wilcox, Asst. Foundry Superintendent, Electric Steel Foundry Co., Portland, Ore.—(Rep. Oregon Chapter and

Steel).

The Secretary was instructed to notify the appointees of the

Executive Committee action and to request their individual acceptances.

There being no further business to come before the Committee, the meeting was declared adjourned.

Respectfully submitted, Wm. W. MALONEY Secretary-Treasurer

Approved:

E. W. HORLEBEIN. President

Minutes Special Meeting 1949-1950 Board of Directors

Palmer House, Chicago-July 28, 1949

Present: President E. W. Horlebein, presiding Vice-President W. L. Woody

Directors:

(Terms exp. 1950) (Terms exp. 1951) (Terms exp. 1952) T. H. Benners, Jr. E. N. Delahunt T. E. Eagan N. J. Dunbeck L. C. Farguhar W. J. MacNeill V. J. Sedlon F. C. Riecks A. M. Fulton F. G. Sefing Robert Gregg J. M. Robb, Jr. L. D. Wright W. B. Wallis V. E. Zang

Secretary-Treasurer Wm. W. Maloney

Technical Director S. C. Massari

Absent: Director A. C. Ziebell

President Horlebein called attention to Art. VII Sec. 1 of the by-laws calling for election of four members of the Board who would serve, together with the President and Vice-President, as an Executive Committee of the Board of Directors. He announced that he desired to place in nomination for the Executive

Committee, names of the following Directors: F. C. Riecks, J. M. Robb, Jr., W. B. Wallis and V. E. Zang.

On motion by Director MacNeill, seconded by Vice-President Woody and carried, the nominations were closed and the nominees named by the President declared unanimously elected members of the Executive Committee.

President Horlebein then stated that in order to save time at the Board meeting to be held July 29, he desired to place in nomination the name of Wm. W. Maloney for re-election as Secretary-Treasurer. On motion by Vice-President Woody, seconded by Director Delahunt and carried, nominations were closed and Wm. W. Maloney declared unanimously re-elected Secretary-Treasurer for the fiscal year 1949-50.

There being no further business to be considered, the meeting was declared recessed until the following morning.

Respectfully submitted, WM. W. MALONEY Secretary-Treasurer

Approved:

E. W. HORLEBEIN, President

Minutes

First Meeting 1949-1950 Board of Directors

Palmer House, Chicago-July 29, 1949

Present: President E. W. Horlebein, presiding Vice-President W. L. Woody

Directors:

(Terms exp. 1950) (Terms exp. 1951) (Terms exp. 1952) E. N. Delahunt T. H. Benners, Jr. T. E. Eagan W. J. MacNeill N. J. Dunbeck L. C. Farquhar V. J. Sedlon F. C. Riecks A. M. Fulton F. G. Sefing J. M. Robb, Jr. Robert Gregg W. B. Wallis V. E. Zang L. D. Wright

Secretary-Treasurer Wm. W. Maloney

Technical Director S. C. Massari

Absent: Director A. C. Ziebell

A quorum having been established, the meeting was called to order.

Fixing of Staff Salaries and Compensations

In Executive Session, with members of the Staff excluded, the Board of Directors approved recommendations of the Finance Committee for Staff salaries and compensations effective July 1, 1949.

1949-50 Budget of Income and Expense

A budget of Income and Expense for the fiscal year 1949-50 was considered in detail, as recommended by the Finance Committee. During the course of the discussions, certain actions were taken as follows, concerned with various items of Income and Expense:

(A) 1950 Convention Registration Fees

Discussion developed unanimous agreement on the part of the Board that a Registration Fee should be charged all attending the Convention, that there should be established a substantial difference between the Member and the non-member registration fees, and that Company and Sustaining Members be permitted to register their plant men in advance at the lowest Member registration rate.

Recommendation of the 1948-49 Board of Directors was considered, calling for a Member registration fee of \$3.00, nonmembers \$7.00. On motion by Director Wright, seconded by Vice-President Woody and carried, these fees were approved for the 1950 Convention.

(B) Exhibit Permit Fee

It was pointed out that the Exhibit Permit Fee, entitling firms to display their products and to do business in the exhibit halls during Convention Week, had been charged for many years. On motion by Director Robb, seconded by Director MacNeill and carried, the Board approved a \$25.00 Exhibit Permit Fee, as in previous years, in connection with the 1950 Exhibit.

(C) Space Rentals

It was pointed out that the item of \$182,000 budgeted for 1950 Exhibit Space Rentals was on the basis of \$2.50 per square foot. On motion by Director MacNeill, seconded by Director Fulton and carried, the Board approved the recommendations of the Finance Committee as stated.

(D) Office Rentals

Recommendations of the 1948-49 Board of Directors were considered, calling for continuation of the present National Office lease until expiration April 30, 1950, with the Staff and Finance Committee jointly authorized to negotiate for new and more economical quarters in the meanwhile. Approved, without vote. On motion by Director MacNeill, seconded by Director Farquhar and carried, the President discharged the Committee on Office Quarters previously appointed, with the thanks of the Board of Directors for its study and recommendations.

(E) Auditing and Accounting

The President pointed out that the Finance Committee was considering, in conjunction with the Society's official auditors, a further study of Society finances with a view toward better ac-

counting and budgetary procedures. He stated that the budgeted Expense item of \$2,650 would fall approximately \$1,500 short of the expenditures necessary for the job. Without vote, it was the consensus of the Board that the Finance Committee should study the recommendations of the official auditors, with authority to proceed and incur the addition \$1,500 estimated, if deemed desirable by the Finance Committee.

(F) American Foundryman Advertising Commissions

It was pointed out that, with the budgeted income of \$115,000 for Display Advertising, the budget of \$16,000 for Advertising Commissions and Discounts probably should be increased by \$1,000. On motion of Director MacNeill, seconded by Director Zang and carried, authorization was given the Finance Committee to so increase this item.

tee to so increase this item.

On motion by Director Riecks, seconded by Vice-President Woody and carried, the 1949-50 Budget of Income and Expense was approved, as recommended by the Finance Committee and as revised in the amount of \$1,000 plus a maximum of \$2,000 for possible employment of the auditors as suggested by the Finance Committee.

(G) The Secretary requested action by the Board of Directors authorizing the issuance of a Power of Attorney to W. H. Alexander, A.F.S. attorney, and Henry S. Grambor of Marsh & McLennan, Chicago, insurance actuaries, to act for the Society in the clearance of A.F.S. Retirement Plan details with the Internal Revenue Department. Motion duly made, seconded and carried.

Appointment of Standing or Special Committees

The President entertained a motion authorizing him to appoint Standing and Special Committees of the Board of Directors as called for by the By-Laws. Motion by Director Gregg, seconded by Director MacNeill and carried. The President announced that he had not as yet completed his thoughts on committee chairmen and personnel, but would announce appointments to the Board of Directors at the earliest opportunity.

Action on Recommendations of 1948-49 Board of Directors

(1) On motion by Director MacNeill, seconded by Director Eagan and carried, recommendations on proposals of the Publications Committee were approved as acted on by the Board of Directors July 28, 1949.

(2) The President called for discussion on the advisability of continuing the Sustaining Membership Campaign authorized in October 1948. On motion by Director Benners, seconded by Director MacNeill and carried, the Board declared that while A.F.S. would continue to accept \$500 Sustaining Memberships, no active campaign would be carried on, pending more intimate study by the Executive Committee.

(3) On motion by Director MacNeill, seconded by Director Fulton and carried, recommendations of the 1948-49 Board of Directors opposing the proposed test-case resolution by the National Castings Council, were approved.

(4) On motion by Director Robb, seconded by Director Dunbeck and carried, recommendations of the 1948-49 Board of Directors were approved dealing with distribution of the Board of Awards Manual.

(5) President Horlebein reported for the Executive Committee on discussions held July 28 with reference to the Society's Safety and Hygiene program. He stated that Technical Director Massari had been instructed to prepare, for the signature of and transmission by Vice-President Woody, a letter to foundry top management in order to develop interest in a more vigorous program. It was understood that recommendations would be made to the Board of Directors by the Executive Committee following circulation of the letter.

(6) Motion was made by Director Eagan, seconded by Director Robb and carried, approving recommendations of the 1948-49 Board of Directors to reaffirm the intention of the Society to stage a Convention and Exhibit in Cleveland May 8-12, 1950. (7) Motion was made by Director Eagan, seconded by Director Robb and carried, approving recommendations of the 1948-49 Board of Directors that the Society stage a Convention and Exhibit in the Spring of 1952, following the Spring 1950 Convention and Exhibit.

It was the consensus of the Board that recommendations on selection of a Convention city for 1952 should be developed by the Exhibits Committee and the Staff, for reporting to the Board of Directors for definite decision in January 1950.

(8) Motion was made by Director Eagan, seconded by Director Wright and carried, referring to the Executive Committee for action at its next meeting, the selection of a non-exhibit city for 1951, with the understanding that this Convention would be held in the Spring of that year.

New Business

Approval of Resolutions

The following resolutions required by the banks and by the Staff for operations during the fiscal year 1949-50 were approved on motions duly made, seconded and carried:

(1) RESOLVED that resolutions required by the Harris Trust & Savings Bank of Chicago, and by the Northern Trust Co. of Chicago, authorizing the withdrawal of funds, are hereby approved and the Secretary authorized to certify thereto.

(2) RESOLVED that checks for the withdrawal of funds deposited in the name of the Society with depository banks, including all general checking accounts and interest savings accounts, and for the withdrawal of all securities held in the various funds of the Society by the Trust Department of the Harris Trust & Savings Bank of Chicago, shall require the signatures of any two of the following Officers: President, Vice-President, Secretary-Treasurer, Technical Director.

(3) Resolved that the Board of Directors authorize a Treasurer's expense account of One Thousand Dollars (\$1,000.00), said account to be reconciled at the end of each month by full statement of expenditures, and withdrawal checks to be signed by any one of the following: President, Vice-President, Secretary-Treasurer, Technical Director.

(4) RESOLVED that the Secretary be authorized to rent a safety deposit box at the Harris Trust & Savings Bank of Chicago for the safe-keeping of Society securities, and that any two of the following have authority to obtain access to such safety box: President, Vice-President, Secretary-Treasurer, Technical Di-

(5) RESOLVED that the Secretary be authorized to reimburse traveling expenses for members in attendance at any regularly called Board of Directors, Executive Committee, or Technical Committee meeting, with the following exceptions: No expenses shall be paid to Directors or Committee members for attendance at meetings held during the week of the Annual Convention of the Society, unless specifically authorized by the Board of Directors. When meetings are held in conjunction with other committees or associations the Secretary is authorized to determine what portion of the expense of such attendance shall be paid by the Society.

(6) RESOLVED that the present Blanket Indemnity Bond be renewed covering all Staff members for Five Thousand Dollars (\$5,000.00), except that Indemnity Bonds be purchased covering the Secretary-Treasurer and Technical Director for Fifty Thousand Dollars (\$50,000.00) each, premiums to be paid by the Society.

(7) RESOLVED that the Secretary be authorized to execute all contracts for the administration of Society affairs, subject to specific approval by the Board of Directors. In the case of A.F.S.-sponsored research projects, approval of the project by the Board of Directors includes the authority for the Secretary to execute contracts for performance of such projects on a bid basis.

(8) RESOLVED that resolutions required by the Royal Bank of Canada, Montreal, Quebec, and by its subsidiary banks at Toronto, Ontario, and Vancouver, B. C., establishing checking accounts in the name of the Society and authorizing the withdrawal of funds therefrom, are hereby approved and the Secretary authorized to certify thereto.

(9) RESOLVED that the Treasurers of the Eastern Canada, Ontario and British Columbia Chapters of the Society are authorized to make deposits, in the Royal Bank of Canada and its subsidiary banks, of funds received from Canadian members in payment solely of membership dues in the Society, and the

Secretary is authorized to make all necessary accounting arrangements.

- (10) RESOLVED that checks for the withdrawal of funds deposited in the name of the Society with Canadian depository banks, shall require the signatures of any two of the following Officers: President, Vice-President, Secretary-Treasurer, Technical Director.
- (11) Resolved that the Treasurers of the Eastern Canada, Ontario and British Columbia Chapters of the Society shall each give evidence to the Secretary of the existence of Indemnity Bonds covering each of them individually and separately, in amounts deemed satisfactory to the Society, premiums thereon to be paid by the respective Chapters.

Expenses of Sand Division Consultant

The Technical Director requested continuation of the Society's previous policy for the retention of Dr. H. Ries as Consultant for the Sand Division. Motion was made by Vice-President Woody, seconded by Director Wright and carried, in approval.

Authoriaztion for A.F.S.-sponsored Research

The Technical Director requested the following sums for continuation of A.F.S.-sponsored research projects during 1950, total budget for such projects being \$33,250.00 as called for by the Budget of Income and Expense:

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| Bras | s & Bron | ze P | re | je | ct | | | | | | | | | . , | | | | | | 5,000 |
| | Iron Pr | | | | | | | | | | | | | | | | | | | |
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On motions duly made, seconded and carried, the expenditure for each individual research project was approved.

1950 Chapter Officers Conference

The Secretary requested authority for staging the 1950 Chapter Officers Conference on the dates of June 26-27. Approved, on motion by Director Wallis, seended by Director Benners and carried.

Discussion took place on issuance of invitations for the 1950 Chapter Conference. Motion was made by Director Robb, seconded by Director Benners and carried, authorizing the issuance of invitations to each Chapter Chairman and Program Chairman, with provision for one alternate, either the Chapter Vice-Chairman or Secretary, as might be approved by the National Secretary.

International Congress Representatives

The Secretary pointed out that it was customary for A.F.S. to have two official representatives in attendance at the International Congress. On motion by Director Wallis, seconded by Director Robb and carried, A. W. Gregg of Whiting Corp., Harvey, Ill., was appointed to serve as the second delegate to the 1949 International Foundry Congress at Amsterdam, along with the A.F.S. European representative, Vincent Delport, of London, England. The Secretary was instructed to notify Mr. Gregg accordingly.

Cooperation with Educational Foundation

Director Sefing requested discussion on the need for A.F.S. to pursue its own Educational Program more vigorously at the secondary school level. He suggested that the Board might delineate more closely its policy with reference to the importance of the various Society activities. The President suggested that this procedure might be carried on by the Executive Committee, reporting back to the Board of Directors at its January 1950 meeting. On motion by Director Sefing, seconded by Director Gregg and carried, the matter was referred to the Executive Committee for further study.

The President then suggested that the following resolution be approved by the Board of Directors and the Secretary instructed to transmit the resolution to the Trustees of the Foundry Educational Foundation:

RESOLVED that the American Foundrymen's Society extends to the Trustees and Staff of the Foundry Educational Foundation its sincere compliments on the progress and accomplishments of the Foundation since its establishment, and at the same time respectfully requests that the Trustees of the Foundation feel free to advise the American Foundrymen's Society what further cooperation the Society may render in extending the work of the Foundation in the future.

On motion by Vice-President Woody, seconded by Director Benners and unanimously carried, resolution adopted.

1950 Nominating Committee

President Horlebein announced the personnel of the 1949-50

Nominating Committee of the Society, as appointed by the Executive Committee. (See Minutes of Executive Committee, July 28, 1949.)

There being no further business to be considered, the meeting was declared adjourned at \$:10 pm.

Respectfully submitted, Wm. W. Maloney Secretary-Treasurer

Approved:

E. W. HORLEBEIN. President

Minutes Meeting of Executive Committee Palmer House, Chicago—October 14, 1949

Present: President E. W. Horlebein, presiding Vice-President W. L. Woody

Directors:

F. C. Riecks Iohn M. Robb, Jr.

V. E. Zang

Secretary-Treasurer Wm. W. Maloney

Technical Director S. C. Massari

Absent: Director W. B. Wallis

The Secretary announced that Minutes of the Board and Executive Committee meetings held July 28-29, 1949, had been approved by letter ballot of the Directors. Accordingly, on motion duly made, seconded and carried, reading of the Minutes was dispensed with.

Membership Report

The Secretary reported that as of September 30, the Society's membership totaled 9,835, a net loss of 228 since July 1, and that total circulation of American Foundryman was 10,094, including 217 subscriptions and 42 other circulation.

It was stated that the increase in New Member applications during July-August had been very low compared with previous years, partly due to vacations and partly to business conditions. The Secretary also pointed out that all delinquents on July 1 renewals were being dropped as of October 31 and this would represent a further decrease in the membership. It was pointed out, however, that the total decrease was still far less than the decrease anticipated in preparation of the 1949-50 financial budget, and that the start of Chapter meetings in September and October was expected to bring in additional new members monthly.

The Secretary cautioned against optimistic membership totals so long as the current steel and coal strikes remain in effect. The Secretary stated that the membership report as of September 30 had been mailed to all Chapter Membership Committee Chairman for their information.

Report on Finances

The Treasurer reported on finances as of September 30, stating that 29.8% of the Income Budget had been earned in the first quarter, and 20.2% of the Expense Budget expended. Total Income during September \$24,267.75; total Expense \$35,944.73. Total Income July 1-September 30, \$169,957.32 (Budget \$570,-275); total Expense \$115,214.02.

The financial statements presented included Balance Sheets for the General Fund and Awards Fund. The General Fund Balance Sheet showed a total of cash and investments on September 30 of

Recommendations of Cupola Research Committee

R. G. McElwee, Chairman of the Cupola Research Steering Committee, entered the meeting on invitation and presented recommendations following a recent meeting of his Committee. The report detailed accomplishments of the Cupola Research Committee to date and its future projects, and recommended that \$50,000 be solicited from industry for continuation of the work. Considerable discussion ensued, it being pointed out by Mr. McElwee that the work to date on coke quality was being materially assisted by industry and much of the work was actually being performed in various industrial plants. He declared that the Committee answers many practical questions brought to them and that the Committee has undertaken extensive studies on air pollution from the cupola in view of the fact that no other agency apparently is undertaking such work.

It was the belief of the Executive Committee that the work on air pollution should be more widely publicized and Mr. Mc-Elwee agreed to present his Committee's findings for publication in AMERICAN FOUNDRYMAN, that these articles would be run over the next six months and that his Committee's recommendations for financial solicitation would be held pending until the development of greater interest during that period of time. It was the consensus that the work on air pollution, once properly publicized, would develop voluntary financial assistance of sufficient extent as to make possible continuation of the work. The consensus also appeared opposed to industrial solicitation at this time in view of prevalent business conditions and imminent solicitation by the Foundry Educational Foundation, in many cases directed toward the same companies which would be asked to contribute to an additional Cupola Fund. Mr. McElwee expressed himself as being quite satisfied with the consideration and attitude of the Executive Committee.

It was the consensus of the Committee that the expenses of Theodore Giszczak should be reimbursed by any plant which requests his presence to assist in solution of some specific foundry problem.

The Secretary was instructed to prepare a letter to the Cupola Committee congratulating them on their past work, stating that the Executive Committee is in sympathy with the Committee's need for funds, but expressing the thought that their work has not been sufficiently publicized to date and suggesting that the Society concentrate on publicity for the Committee during the next six months, which publicity, if properly handled, would, in the estimation of the Executive Committee, result in funds being forthcoming voluntarily.

Report on West Coast Chapter Visits

President Horlebein reported briefly on his visit to the five West Coast Chapters, commencing with the British Columbia Chapter and ending with the Southern California Chapter. He stated he had attended Board and regular Chapter meetings with the three Pacific Northwest groups and Board meetings only with the California groups. He brought out the fact that all the West Coast Chapters now have agreed to arrange their meeting schedules, beginning in the Fall of 1950, so as to make it possible for Eastern speakers to address all five Chapters in minimum time. He also stated that there was considerable interest in developing Regional Conference in Pacific Northwest.

The Secretary mentioned that Technical Director Massari was scheduled to speak at the Pacific Northwest Chapters this Fall, and Mr. Massari left the meeting early to commence his trip.

Report of Publications Committee

The Secretary presented a report of the Publications Committee, briefed as follows:

(a) Book Inventories. No necessity for revision of present inventories in view of present sales.

(b) Textbooks. Manuscripts not yet received, but first one promised shortly after January 1, 1950. No definite commitments yet made for outside publication, although the definite intention is to have textbooks published by an outside publishing house.

(c) Foundry Pamphlet. The Executive Committee approved the recommendation of the Publications Committee for preparation of a foundry indoctrination pamphlet to be placed in the hands of large foundry firms for indoctrinating beginning foundry employees. The recommendation will be presented to the Educational Division for consideration.

(d) American Foundryman. Recommendation of the Publications Committee was considered.

"That a more aggressive program for increasing advertising in American Foundryman be developed in order to make possible the publication of additional good editorial material."

The Executive Committee concurred with the recommendation of the Publications Committee

"THAT AMERICAN FOUNDRYMAN obtain and publish good articles on any phase of metal casting, including die casting and heat and corrosion resistant alloys."

(e) Publication policies. The Executive Committee was informed of the desire of the Publications Committee that all policies regarding American Foundryman to date be itemized and made a part of the Publications Committee procedures. The Executive Committee also was informed of Publications Committee action to the end that Chairman of said Committee should serve on the Technical Correlations Committee in order to become better acquainted with Divisional publication plans. The Executive Committee concurred without vote.

(f) Revolving fund. The Executive Committee was acquainted with discussion of the Publications Committee and the latter's feeling that a revolving publication work was felt to be unnecessary, as well as the Publications Committee's recommendation that a two-year Society budget might be advisable for the purpose of better planning of publication schedules.

(g) Recommendation of the Publications Committee was presented for approval of publication of a Bibliography of Centrifugal Castings. Motion for approval duly made, seconded and carried.

Selection of 1951 Convention City

Convention & Exhibits Manager A. A. Hilbron entered the meeting for purposes of the discussion. The Secretary stated that invitations for the 1951 non-exhibit Convention had been received from Montreal, Houston and Buffalo, in that order, although an earlier invitatio. had been received from Buffalo for an Exhibit Convention, this invitation being changed to a non-Exhibit Convention at the 1949 Chapter Officers Conference. The Secretary stated that Convention Manager Hilbron had visited all three cities and had presented a report indicating that all three cities were enthusiastic for the Convention and that all had ample meeting room and hotel room facilities for handling such an event. Following discussion of all invitations, motion was made by Director Riecks, seconded by Director Robb and unanimously carried

THAT the invitation of the Western New York Chapter be accepted and the 1951 non-exhibit Convention of the Society be held in Buffalo on available Spring dates.

The Secretary was instructed to communicate the the action of the Executive Committee to the Eastern Canada Chapter at Montreal and the Texas Chapter at Houston.

Recommendations of Finance Committee

Recommendations of Finance Committee meetings held August 11 and October 13 were considered in detail and the following decisions approved on motions duly made, seconded and carried:

(a) Combining the General Reserve Fund and the General Fund of the Society, utilization of all securities still remaining subject to specific actions by the Board of Directors.

(b) Combining the Cupola Research Fund and General Fund of the Society, differentiations to be arrived at annually through proper bookkeeping records.

at annually through proper bookkeeping records.

(c) The Board of Directors to specify how interest earned on Society securities shall be charged to the various funds annually, following accumulation in one General Fund Checking Account.

(d) Retirement Funds of the Society not to be combined with other Society funds.

(e) A material reduction in the number of bank accounts, both checking and savings, maintained by the Society, including elimination of the Expense Account.

(f) All A.F.S. securities to be held in one Trust Fund Account at the Harris Trust & Savings Bank, interest being identified through bookkeeping entries.

(g) As an excess of cash may accumulate, the Treasurer shall inform the Finance Committee to recommend prompt investment in Board-approved securities.

(h) The Staff to work out in conjunction with the official auditors, and install promptly, approved procedures for better accounting control of membership dues records, expansion of the Manual of Accounts, revision of bookkeeping procedures in accordance with accepted accounting practice, development of greater detail with regard to fund transfers and appropriations when voted, and an allowance of more time and administrative supervision in closing the Society books at the end of the year. In this connection, the official auditors were authorized to set up the financial books of the Society in accordance with generally accepted accounting practice, all details to be worked out jointly with the Staff and Finance Committee at the earliest possible moment.

President Horlebein reported that the entire financial books of the Society were being revised in accordance with recommendations of the official auditors, to conform in every detail to accepted accounting practice. The Secretary stated that the Staff would follow through with the auditors' recommendations in every detail and that the Society's books and accounting procedures would be established precisely in accordance with said recommendations.

Bank Resolutions

The Secretary stated that a number of Staff members had expressed the desire for monthly payroll deductions in order to purchase U. S. Government Bonds on a monthly savings plan. He stated that the Administrative Officer approved the plan and was prepared to establish a separate bank account for the deposit of such savings toward bond purchases, and requested approval of resolutions required by the bank of deposit. On motion duly made, seconded and carried, resolutions were approved.

Educational Program

President Horlebein requested the reading of a letter from Director F. G. Sefing, requesting that the Board of Directors indicate the activities of the Society in order of importance to the end that expenditures of the Society may be made accordingly. President Horlebein stated that the Board should constantly be aware of the relative importance of the Society's activities and might well evaluate such activities each year in its discussions and budget-making. Motion was made by Vice-President Woody, seconded by Director Riecks and carried, recommending

THAT all activities of the Society, including those already undertaken and those projected, be listed and submitted to all Directors for the recording of their ideas of relative importance, for discussion at the January 1950 meeting of the Board of Directors.

Safety and Hygiene

Vice-President Woody presented a brief verbal progress report on a letter directed to top management of some 25 large foundry companies, requesting their reaction to an extended A.F.S. program of Safety and Hygiene and their willingness to contribute thereto in amounts to be determined. He stated that while some encouraging response had been received, a meeting of the Safety and Hygiene Committee thus far constituted would be held at an early date for full discussion.

Exhibit Policies

President Horlebein presented correspondence exchanged between himself and W. L. Dean, President of the Foundry Equipment Manufacturers Association, in response to the latter's

request for additional details on present A.F.S. exhibit policy. It being suggested that President Horlebein's reply might well form the basis of a stated Board policy, the matter was referred to the January 1950 meeting of the Board of Directors.

There being no further business to be presented the meeting was declared adjourned.

> Respectfully submitted, WM. W. MALONEY Secretary-Treasurer

Approved:

E. W. HORLEBEIN, President

Minutes Meeting of Executive Committee Palmer House, Chicago-Dec. 13, 1949

President E. W. Horlebein, presiding Vice-President W. L. Woody

Directors:

F. C. Riecks John M. Robb, Jr. W. B. Wallis V. E. Zang

Secretary-Treasurer Wm. W. Maloney

Technical Director S. C. Massari

No minutes were read at this meeting, the President indicating that he desired as much time as possible for considering a suggested approach to long-range planning for the Society.

At the outset, President Horlebein read a preamble indicating some concern that the activities of the Society have long been conducted on a hand-to-mouth basis with little or no Board policies established for guidance of each successive Board and the National Office. He outlined his belief that a sound and permanent financial policy for the Society should have four fundamental objectives in mind:

(1) Development of a financial structure that will enable balancing the budget annually, without the necessity for appropriations from either Exhibit surpluses or General Reserve funds.

(2) Creation of financial means that will enable the continuous accumulation of reserves deemed adequate for a society of progressive nature and scope.

(3) Assurance of adequate funds for continuing (and conservatively broadening) those activities of A.F.S. considered essential, without recurrent solicitations of contributions from the industry.

(4) Establishment of overall financial and administrative policies so as to facilitate long-range planning of activities with some reasonable assurance of program continuity, for the guidance of the Staff, the Chapters, the Technical Divisions, and, by successive recommendation and adoption, of future Boards of Directors.

The entire day was given over to discussing the various questions raised, among them being the following:
(1) Minimum Goal of Society reserve funds.

(2) The question of maximum membership believed possible.

(3) Relation between activities and budgeting for non-Exhibit as well as Exhibit years.

(4) A dues structure based on maximum potential membership.

(5) Relations with other foundry associations.

A great deal of factual information was presented to assist the Executive Committee in its discussions. Members of the Committee felt that the material submitted deserved more comprehensive study toward the end of policy-making and in concluding, all were requested to study the material carefully in order to carry on further discussions at a later date.

There being no further business to come before the Executive Committee, the meeting was declared adjourned to reconvene on

January 26, 1950.

Respectfully submitted, WM. W. MALONEY Secretary-Treasurer

Approved:

E. W. HORLEBEIN, President

Minutes Meeting of Executive Committee Palmer House, Chicago-January 26, 1950

Present: President E. W. Horlebein, presiding Vice-President W. L. Woody

Directors:

F. C. Riecks John M. Robb, Jr. W. B. Wallis V. E. Zang

Secretary-Treasurer Wm. W. Maloney

Technical Director S. C. Massari

Minutes of the Executive Committee meeting held December 13, 1949, were read, and on motion duly made, seconded and carried, were approved.

"American Foundryman" Advertising

On invitation, Mr. H. J. Fitzpatrick of Higgins & Associates Inc., Chicago, entered the meeting for impartial discussion of ways and means for improving the advertising content of AMERI-CAN FOUNDRYMAN. All members of the Executive Committee entered the discussion and asked questions concerning circulation, advertising rates, distribution, contacts coverage, etc. Mr. Fitzpatrick was then excused, President Horlebein expressing thanks for his courtesy in advising with the Executive Com-

Following some further discussion, motion was made by Director Robb and seconded by Vice-President Woody, and carried, recommending to the Board of Directors

That the Secretary be authorized to employ, subject to approval of the President, publisher representatives for one year to June 30, 1951, such representatives to be responsible for all advertising solicitation in American Foundryman, maximum cost not to exceed \$10,000 for the balance of the current fiscal year to June 30, 1950.

It was understood that division of territories for the solicitation of advertising would be agreed upon jointly between the Secretary, the President and the Publisher Representatives. It was further understood, without vote, that the present compensation of the Advertising Manager would not be reduced in view of reassignment to advertising supervisory work not involving direct solicitation of accounts.

1952 Convention City

The Secretary pointed out the neessity for prompt decision on selection of a location for the International Foundry Congress in 1952, stressing the point that such decision had been deferred at the July, 1949 Board meetings for action at this time after consultation with the Exhibitors Committee.

Director Wallis reported that the Exhibitors Committee had discussed the matter informally and indicated certain preferences. President Horlebein then asked the Secretary to outline available facilities at Cleveland, Chicago, Detroit, New York, Philadelphia and Atlantic City. He emphasized the importance of adequate hotel accommodations for an International Congress, and stated that these cities lined up as follows for adequacy of various facilities: For Exhibits—Cleveland, Atlantic City and Philadelphia; for Housing—Chicago, New York, Atlantic City and Philadelphia; for meeting rooms—Cleveland, and Atlantic City; for Plant Visitations—Cleveland, Chicago, Detroit, New York and Philadelphia.

The Secretary presented housing proposals from Cleveland, Philadelphia and Atlantic City, and it was agreed that only these three cities should be considered. It was understood that pre-Convention and post-Convention tours would be arranged for foreign visitors, which might diminish the importance of plant visitations. It was stated that no Chapter invitations for 1952 had been received. President Horlebein reported on unsuccessful efforts to increase room guarantees from Cleveland hotels.

On the basis of the facts presented, and in the belief that housing accommodations would be of great importance in staging a successful International Congress in 1952, motion was made by Director Zang, seconded by Director Wallis and unanimously carried, recommending to the Board of Directors that Atlantic City be selected as the 1952 Convention City.

Board Policies

President Horlebein presented to the Executive Committee a plan for defining more clearly and precisely Policies of the Board for guidance of Society bodies and affairs, and in order to obtain greater interest in Society activities by individual Board members through more active participation in establishing polices. Members of the Executive Committee commented at length on the proposals, and expressed themselves in agreement with the underlying aim of more clearly defining Board policies on various activities, reaffirming or revising existing policies and establishing policies where need exists. They cautioned, however, against setting up too complex a structure to accomplish the basic purpose, fearing duplication of effort and possible confusion of aims and jurisdictions.

No action ensued, President Horlebein stating that he desired full discussion by the Board of Directors in line with the aim of greater Board participation and interest.

Safety and Hygiene

Vice-President Woody reported on progress made, as authorized by the July Board meetings, in developing a Safety and Hygiene program acceptable to the Foundry Industry. He stated that representatives of large foundry organizations did meet in Chicago on December 9, 1949, and after complete discussion had recommended that A.F.S. undertake a strong program involving: Solicitation of funds totaling \$50,000 a year for ten years, a paid Staff man to correlate the work, and development of specified codes of recommended practice at the outset. Vice-President Woody expressed the belief that sufficient interest on the part of the industry had been evidenced to warrant A.F.S. going ahead.

Vice-President Woody then stated that he would recommend to the Board of Directors the following day, that solicitation of funds for a ten-year Safety and Hygiene program be approved to commence approximately September, 1950, that a sum of \$35,000 a year be set as the goal, that such funds be earmarked for the porgram, and that a competent full-time Staff man be sought to commence work in the near future. Without vote, the Executive Committee agreed to such a recommendation.

(Note: Other actions of Executive Committee incorporated in Minutes of Board of Directors meeting January 27, 1950.)

Respectfully submitted, WM. W. MALONEY Secretary-Treasurer

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Approved:

E. W. HORLEBEIN, President

Minutes

Mid-Year Meeting of Board of Directors

Palmer House, Chicago-January 27, 1950

Present: President E. W. Horlebein, presiding Vice-President W. L. Woody

Directors:

W. J. MacNeill T. H. Benners, Jr. F. C. Riecks N. J. Dunbeck John M. Robb, Jr. Robert Gregg W. B. Wallis V. E. Zang F. G. Sefing L. D. Wright

Secretary-Treasurer Wm. W. Maloney

Technical Director S. C. Massari

Absent: Directors E. N. Delahunt, A. C. Ziebell, A. M. Fulton.

Reading of Minutes

The Secretary stated that Minutes of the Board of Directors meeting held July 29, 1949, had been approved by letter ballot of the Directors. By common consent, their reading was dispensed with.

The Secretary stated that actions of the Finance Committee, in meeting held August 12, 1949, were incorporated, for approval and progressive development, in the Minutes of the Executive Committee meeting held October 14, 1949. The latter Minutes were read in full and, on motion duly made, seconded and carried, were approved.

Minutes of the Executive Committee meeting held December 13, 1949, were read and, on motion duly made, seconded and carried, were approved.

Report of Secretary

The Secretary reported in full on Society activities for the sixmonth period ended December 31, 1949. Report accepted, on motion duly made, seconded and carried.

Report of Treasurer

The Treasurer's report of Society finances for the six-month period ended December 31, 1949, was read in full and accepted on motion duly made, seconded and carried.

Report of Technical Director

The Technical Director reported in full on technical activities of the Society for the six-month period ended December 31, 1949. His report was accepted, on motion duly made, seconded and carried.

Director MacNeill specifically requested information on the usefulness of the projected College Text Book for in-plant instruction of graduate engineers. The Technical Director replied that several foundry organizations had already expressed intention of adopting the book for that purpose.

Report of Nominating Committee

The report of the 1949-50 Nominating Committee, requiring no approval by the Board under the by-laws, was presented by the Secretary for purposes of information and record, nominees selected being as follows:

For President, to serve one year—Vice-President W. L. Woody.
For Vice-President, to serve one year—Past-Director Walter
L. Seelbach, President, Superior Foundry, Inc., Cleveland.

For Directors, each to serve three years:

J. J. McFayden, General Manager, Galt Malleable Iron Co., Galt, Ont., Canada.

J. O. Ostergren, President, Lakey Foundry & Machine Co., Muskegon, Mich.

F. W. Shipley, Foundry Manager, Caterpillar Tractor Co., Peoria, Ill.

Jas. Thomson, Chief Engineer, Continental Foundry & Machine Co., East Chicago, Ind.

E. C. Troy, Foundry Engineer, National Engineering Co., Philadelphia.

For Director, to serve one year—President E. W. Horlebein, on completion of present term of office (as prescribed in A.F.S. by-laws).

1950 Convention and Exhibit

The Secretary and Technical Director presented brief progress reports on the various events being organized for the Foundry Convention and Show. Director Sedlon announced steps toward preparation of a special exhibit of the Patternmaking Division to show modern patternmaking materials and methods, and expressed belief the display would add greatly to Patternmaking Division enthusiasm.

Recommendations of Finance Committee

New Office Quarters. Recommendation was presented that the Secretary be authorized to execute a lease for new National Office quarters at 616 S. Michigan Ave., at a considerably reduced rental, lease to run from May 1, 1950 to September 30, 1953. On motion by Director Eagan, seconded by Director Benners and carried, recommendation approved.

Budget Revisions. The President stated that the Finance Committee had compared actual and projected amounts with each item of the Income and Expense budget but saw no reason for revision of total budgetary figures at this time.

Recommendations of Executive Committee

Action on Recommendations of Board of Awards. Recommendations of the Board of Awards, in meeting December 8, 1949, for presentation of Gold Medals and Honorary Life Memberships at the 1950 Convention were discussed, it being pointed out that such recommendations require approval by the Board of Directors under the Society's By-Laws. On motions duly made, seconded and carried, the following recommendations were unanimously approved:

To CLARENCE H. LORIG, Battelle Memorial Institute, Columbus, Ohio—the Joseph S. Seaman Gold Medal of A.F.S. "for his many contributions to foundry metallurgy and for his constant stimulation of research in

the cast metals field."

To Richard Schneidewind, Prof., Met. Engrg., University of Michigan, Ann Arbor, Mich.—the Wm. H. McFadden Gold Medal of A.F.S. "for his valuable contributions to the Malleable Iron Industry in connection with the graphitization of white cast iron and for his many contributions to the Society and the Castings Industry."

To R. L. Lee, General Motors Corp., Detroit-Honorary Life Membership in A.F.S. "for his philosophical approach to the problems of the Foundry Industry and for his stimulation of pride of craftsmanship among foundrymen."

To CLYDE L. FREAR, U. S. Bureau of Ships, Washington, D. C.—Honorary Life Membership in A.F.S. "for his long-time contributions to the Society and for his work in coordinating the casting processes with Naval requirements."

To E. W. HORLEBEIN, Honorary Life Membership in A.F.S. on completion of his present term of office as President.

"American Foundryman" Advertising. Recommendation presented calling for appointment of publisher representatives for solicitation of further advertising in AMERICAN FOUNDRYMAN in line with previous recommendation of Publications Committee for "a more aggressive advertising policy." President Horlebein pointed out that recommendation covered only the period to June 30, 1951, and declared that the recommendation contemplated due consideration for compensation of the existing Advertising Manager. He called attention to a contemplated maximum expenditure of \$10,000 for current fiscal year in the way of additional commissions to advertising representatives, selection of representatives and division of territories to be made jointly by the President and Secretary.

On motion by Director Eagan, seconded by Director Sedlon and carried, recommendations of the Executive Committee were

unanimously approved.

1952 Convention City. President Horlebein offered the recommendation of the Executive Committee for selection of Atlantic City for the International Foundry Congress in 1952. Being presented with all facts supporting such recommendations, the Directors expressed approval, without vote, in the belief that the matter should be discussed with the Committee of Exhibitors before taking final action. Accordingly, motion was made by Director Dunbeck, seconded by Director Wallis and carried

THAT final determination of a 1952 International Foundry Congress city be left to the discretion of the President and Secretary, in view of the need for further exploration of the subject.

Exhibit Policy. Recommendation was presented, that letter of President Horlebein dated October 5, 1949, addressed to the President of the FEMA, be adopted as expressing the policy of A.F.S. on the staging of Exhibits in conjunction with Conventions of the Society. On motion by Director Gregg, seconded by Director Wright and carried, the recommendation was accepted, incorporating the following letter of President Horlebein into the records as established policy:

"That succeeding Presidents appoint two or three members of his Board to serve with himself and the Secretary as a committee to

"(1) Meet with representatives of the equipment and supply organizations and any others who might be affected to discuss any contemplated material departure from previous procedure regarding the A.F.S. Exhibit Conventions.

"(2) The A.F.S. representatives to be mainly concerned in placing before this group, in concrete form, the thoughts and reasoning of the A.F.S. Board pertaining to a proposed deviation from previous practice, and then to prepare for presentation to the A.F.S. Board a complete and detailed summary of all the facts, both for and against and plus any suggestions thus accumulated as a result of such a meeting.

"(3) The representatives of the A.F.S. would also make themselves available if a request for a meeting originates from outside the Society on some important

matter pertaining to our Exhibits.

"(4) The Board, after receipt of its representatives' complete report, would carefully weigh the facts as presented and determine what action should be taken. It is their decision to make, courageously, intelligently, and with a firm desire to act in the best interests of the industry."

Safety and Hygiene. Recommendation presented calling for establishment of a ten-year A.F.S. program of Safety and Hygiene,

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solicitation from industry of funds totaling \$35,000 a year, employment of a full-time qualified Staff man for the project, and outlining specific original codes of recommended practice to be developed and revised. Vice-President Woody stated the intention to commence solicitation of funds approximately October, 1950, and that the first solicitation would be of Company and Sustaining members on some agreed basis.

On motion by Director Eagan, seconded by Director Gregg and carried, recommendations were approved as presented.

Cupola Research. Recommendation presented calling for continuation of Cupola Research work beyond June 30, 1950 (at which time it is estimated present funds will be practically exhausted) by solicitation of funds totaling \$10,000 a year for a ten-year program, such solicitation to be combined with the solicitation of Safety and Hygiene funds and to run concurrently therewith, to begin not later than September, 1950.

Directors Eagan and Sefing expressed themselves as opposed to providing additional funds until the Cupola Research project shall have demonstrated results from usage of funds previously provided. It was pointed out by the Technical Director that considerable sums had been expended on an organized search of the literature for cupola operations references, on preparation of the Cupola Handbook, and on a full-time paid research worker over the past 18 months. He stated that a number of worthwhile reports had been presented at previous A.F.S. Conventions and in American Foundryman and that important studies on coke behavior and slag analyses were under way, even though temporarily retarded by coke shortages and by inability to consistently secure plant facilities for extensive work on slags.

President Horlebein stated that R. G. McElwee, Chairman of Cupola Research Steering Committee, had been invited to present recommendations for funds at the Executive Committee meeting held October 14, at which time it was agreed that a comprehensive report of activities and accomplishments would be prepared. He stated that this report would become the basis for the solicitation of the additional funds recommended.

Several Directors expressed the belief that further work on Cupola Research might very well be carried on under the direct jurisdiction of the A.F.S. Technical Director, in the interests of maximum efficiency.

Following discussion, motion was made by Vice-President Woody and seconded by Director Dunbeck

THAT the recommendations of the Executive Committee be approved for solicitation of additional Cupola Research funds to a total of at least \$10,000 a year for a ten-year program, and that such solicitation be combined with the solicitation of Safety and Hygiene funds for a similar period.

Vote on the above motion was conducted by a showing of hands with the following result: Yeas 11, Nays 3. President Horlebein declared the motion carried.

Policy Making. President Horlebein presented a plan for establishing more definite policies of the Board for guidance of

all A.F.S. bodies, and in order to develop greater interest of and activity by members of the Board on the affairs of the Society. Following full discussion, motion was made by Director Eagan, seconded by Director Sefing and carried, authorizing the President to appoint Committees of the Board for the establishing of more definite policies for governing the affairs of the Society.

President Horlebein stated that a Board Policy Steering Committee would be appointed by himself at an early date to set up the program as contemplated.

Malleable Research Project. Recommendations presented for approval of a new Malleable Research project on "A fundamental laboratory study of the effect of melting conditions and particularly atmosphere above the melt, and its relation to the behavior of the resulting white iron," maximum expenditure \$5,000 a year, to commence with the 1950-51 fiscal year. It was stated that the former Malleable Research project on "A study to establish the most suitable microstructure for the selective hardening of pearlitic malleable iron castings" has been completed, final report to be presented at the 1950 Convention.

On motion by Director Riecks, seconded by Director Eagan and carried, recommendation approved:

New "Fluid Flow" Film. Recommendation presented calling for maximum estimated expenditure of \$1,500 for a new sound film on "Fluid Flow in Transparent Molds," developed by Aluminum & Magnesium Division in connection with its current research project. In view of highly successful results and recognition of first film, motion was made by Director Eagan, seconded by Director Farquhar and carried, approving recommended expenditure.

A.F.S. Projector Equipment. Recommendation presented for estimated maximum expenditure of \$750 for silent-sound projector equipment for National Office use, for inspection of existing A.F.S. films and review of films preferred for Chapter presentation. On motion by Director Sedlon, seconded by Director Riecks and carried, recommendation of expenditure was approved.

Priority of A.F.S. Activities. No recommendations offered, the Secretary presenting for informative purposes a summary of results from a Board questionnaire on priority of A.F.S. activities, insofar as expenditure of funds may be concerned. No action resulted.

Sustaining Membership Campaign. Opinion of Executive Committee expressed that Sustaining Membership Campaign be not renewed at this time, until business conditions appear more favorable, and in view of contemplated solicitation of funds for safety and Hygiene and Cupola Research. The Board concurred, without vote.

There being no further business to be considered, the meeting was declared adjourned at 4:40 pm.

Respectfully submitted, Wm. W. MALONEY Secretary-Treasurer

Approved: E. W. HORLEBEIN, President

Minutes Annual Meeting 1949-1950 Board of Directors

Drake Hotel, Chicago-Thursday, July 27, 1950

Present: President E. W. Horlebein, presiding Vice-President W. L. Woody

Directors:

(Terms exp. 1950) (Terms exp. 1951) (Terms exp. 1952) T. H. Benners, Jr. E. N. Delahunt T. E. Eagan N. J. Dunbeck L. C. Farquhar W. J. MacNeill F. C. Riecks Robert Gregg V. J. Sedlon John M. Robb, Jr. M. J. O'Brien, Jr. F. G. Sefing W. B. Wallis V. E. Zang L. D. Wright

Secretary-Treasurer Wm. W. Maloney Technical Director S. C. Massari Absent: Director A. C. Ziebell

Observers: Vice-President-elect Walter L. Seelbach

Directors-elect J. J. McFadyen F. W. Shipley

F. W. Shipley James Thomson E. C. Troy

Absent: Director-elect J. O. Ostergren

Reading of Minutes

Minutes of the January 27, 1950 meeting of the Board of Directors having been approved by letter ballot, motion was duly made, seconded and carried, dispensing with their reading.

Minutes of the Finance Committee meeting held April 14, 1950 were read, and on motion duly made, seconded and carried, accepted.

Reports of Staff Officers

Reports of the Secretary-Treasurer and of the Technical Director for the fiscal year were presented and, on motions duly made, seconded and carried, were accepted with commendation. These reports are shown elsewhere, separately, in this volume.

Reports of Committees

National Castings Council

Vice-President Woody reported on meetings of the National Castings Council held during 1949-50 and stated that it was the intention of the Society to continue its representation on this body.

Publications Committee

Minutes of the Publications Committee meeting held June 7 were presented and approval given certain recommendations of this Committee, as follows:

(1) The Board approved a recommendation that the selling price of the book *Development of the Metal Castings Industry* be reduced to both members and non-members, that the Educational discount to libraries and educational institutions be materially increased and that distribution to libraries and schools be energetically pursued.

(2) The National Office was authorized to dispose of 1,000 copies of the Code of Recommended Practices for Industrial Housekeeping and Sanitation originally published in 1943, in

order to relieve inventories.

(3) The Board approved a recommendation of the Publications Committee that A.F.S.-sponsored research progress reports be distributed gratis to Sustaining members of the Society in advance of other distribution and that the membership be made aware of his policy.

(4) Recommendations of the Committee on Publications to be produced during 1950-51 were tabled for consideration by the

incoming Board of Directors on July 28.

- (5) Recommendations of the Publications Committee that an additional full-time Technical Assistant be employed as a member of the Staff to expedite publications and other technical matters, particularly to complete a revision of the Cast Metals Handbook, were tabled for consideration by the new Board on July 28.
- (6) Recommendations of the Publications Committee were approved for establishment of new pricing formula for publications completely produced in the National Office of the Society.

Recommendations of the Finance Committee

Recommendations of the Finance Committee meeting held April 14, 1950 were considered in detail and the following actions taken:

(1) Proposal that A.F.S. invest approximately \$50,000 in Surplus Society funds in Securities other than U. S. Government Securities, particularly better interest-bearing securities, by employment of the Investment Agent Service of the Harris Trust & Savings Bank of Chicago. On motion duly made, seconded and carried, the recommendation was unanimously approved and the Secretary authorized to negotiate a contract accordingly. It was understood that a list of securities recommended for purchase is to be submitted by the Investment Agent Service employed, for the approval of the Finance Committee.

(2) Recommendation for approval of expenditures covering the purchase of reproduction equipment for the National Office were approved, on motion duly made, seconded and carried.

(3) At the suggestion of President Horlebein, and on motion duly made and seconded, the following resolution was unanimously adopted:

RESOLVED that the Finance Committee be authorized to approve expenditures not to exceed \$1,000 without the necessity for subsequent Board approval.

Committee on Exhibits

President Horlebein recalled the lengthy discussions at the January 1950 meeting of the Board concerning a location for the

1952 Convention and Exhibit of A.F.S. At that time, the Board directed that final decision on the matter be left to the President and the Secretary, with the understanding that further discussions would be held at Atlantic City and thereafter with the Committee on Exhibits.

The President stated that he had personally visited Atlantic City with the Secretary and had investigated details of the subjects discussed in the previous Board meeting, and that thereafter on February 27, he and past-President Wallis met with a Committee of Exhibitor representatives in Chicago. At that meeting, the President reported, the Society's representatives had discussed openly and in detail the problem of obtaining all facilities considered essential to the holding of an International Foundry Congress in 1952. Full discussion was entered, and in conclusion, the Exhibitor representatives in Chicago. At that meeting, the President reported, the Society's representatives had discussed openly and in detail the problem of obtaining all facilities considered essential to the holding of an International Foundry Congress in 1952. Full discussion was entered, and in conclusion, the Exhibitor representatives present expressed their belief "That it seemed advisable to hold the International Foundry Congress in 1952 in Atlantic City, considering all facilities required, and that a seven-day Show, from Thursday, May 1 to the following Wednesday seemed the best arrangement."

The Exhibitors representing the Foundry Equipment Manufacturers' Association and the Foundry Facings Manufacturers' Group who were present at the meeting, agreed to notify their respective members at the proper time, and to indicate their agreement to a meeting in Atlantic City. In turn, A.F.S. agreed to work closely with the Exhibitors and to keep Exhibitors con-

stantly advised of developments.

The Secretary then reported that he and Convention Exhibit Manager Hilbron had spent three days in Atlantic City June 12-14 and had gone over in complete detail all facilities, costs and requirements involved. As a result, the Secretary recommended to the President, that Atlantic City be selected for the 1952 International.

In turn, President Horlebein thereupon stated that he was satisfied that Atlantic City offered the best complete facilities for the 1952 International Foundry Congress and Exhibit, and as a recommendation, requested ratification by the Board of Directors. On motion by past-President Wallis, seconded by Vice-President Woody, and unanimously carried, the Board ratified the President's decision.

Board Policy Steering Committee

Minutes of the meeting of the Board Policy Steering Committee held in Chicago March 17, 1950 were read, and the following actions taken:

(1) The Board ratified the invitation previously extended to Directors of the Society to attend the 1950 Chapter Officers Conference June 26-27.

(2) The Board ratified the action of the Board Policy Steering Committee in inviting 1949 and 1950 Directors to attend a Board orientation meeting in Chicago July 26, 1950, in advance of the Annual Board Meeting.

(3) The Secretary was directed to send a copy of the Board Policy Steering Committee Minutes to incoming members of the Board of Directors.

Safety and Hygiene

Discussions on Safety and Hygiene and Cupola Research were tabled for full consideration by the 1950-51 Board of Directors, in meeting July 28.

Resolutions

President Horlebein called attention to the recent deaths of past-President Thomas S. Hammond, Educational Division Chairman, A. W. Gregg, and Cupola Research Project Chairman, R. G. McElwee. On motions duly made, seconded and unanimously carried, appropriate resolutions were directed to be spread upon the Minutes of the Board of Directors meeting, with a copy to the families and business associates of the deceased.

Conclusion

In clesing the meeting, President Horlebein again urged organization of the Board of Directors to the end that the Directors, the Membership and the Staff might work as a team for the progress of the Society and the industry, and emphasized the need for setting down Board policies in written form for the better direction of the Board, the Staff and the Membership.

Vice-President Woody responded by expressing to the outgoing President and Directors the appreciation of the Society for their work of the past years, their active participation in Chapter contacts, and expressed particularly the thanks of the Society for the great energy and effort expended on behalf of A.F.S. by President Horlebein. He stated that as President he would endeavor, to the best of his ability, to carry on the work of his predecessors.

The Annual Meeting of the 1949-50 Board closed the evening of July 27 with the Third Annual Dinner in honor of the retir-

ing Directors. Guests of honor included retiring Director and past-President W. B. Wallis, and retiring Directors E. N. Delahunt, W. J. MacNeill, F. C. Riecks, John Robb, Jr. Due to illness, retiring Director A. C. Ziebell was unable to be present.

There being no further business to come before the meeting, President Horlebein declared the 1949-50 Board of Directors meeting was adjourned, and turned over the gavel of the Chairman to Mr. Woody.

Respectfully submitted, WM. W. MALONEY Secretary-Treasurer

Approved:

E. W. HORLEBEIN, Chairman

Seventh Annual Chapter Officers Conference June 26 and 27, 1950 - Stevens Hotel, Chicago

PROGRAM

| | Monday, June 26 | , | Tuesday, June 27 |
|----------|---|--------------|---|
| 10:00 AM | Welcome to Chapter Officers Chairman W. L. Woody | 9:00 AM | Roll Call |
| | Introductions A.F.S. BackgroundA.F.S. Pres. E. W. Horlebein | | AMERICAN FOUNDRYMAN Editor H. F. Scobie Adv. Mgr. T. B. Koeller |
| 10:30 AM | Functions of the National Office | 9:45 | Membership Work |
| | Secretary Wm. W. Maloney | 11:00 | Chapter Educational Work |
| | "Booby Traps for the Chairman") Quinn B. Brishand | 12:30 PM | Luncheon |
| | How to Act Like a Chairman Quinn R. Prichard | 1:30 | Report on FEF ProgressG. K. Dreher |
| 12:30 PM | Luncheon | 2:00 | Conventions & Exhibits. Exhibits Mgr. A. A. Hilbron |
| 1:30 | A.F.S. Technical ActivitiesTech. Dir. S. C. Massari | | National Nominations and Elections |
| 2:30 | A.F.S. Building Project Chairman W. L. Woody | | Secretary Wm. W. Maloney |
| 3:45 | Chapter Program Building | | A.F.S. Awards |
| 6:00 | Dinner | | A.F.S. Finances Treasurer Wm. W. Maloney |
| 7:00 | "Let's Pretend" | 2:30 3:30 | The Shakeout Adjournment |

ATTENDANCE

- BIRMINGHAM-Chairman Morris L. Hawkins, Stockham Valves & Fittings, Inc., Birmingham, Ala.
- British Columbia—Chairman Lovick P. Young, A-1 Steel & Iron Foundry, Ltd., Vancouver, B. C., and Program Chairman W. M. Armstrong, University of British Columbia, Vancouver, B. C. (Also V. C.)
- CANTON DISTRICT—Chairman T. W. Harvey, Pittsburgh Valve & Fittings Div., Pitcairn Co., Barberton, Ohio, and Program Chairman Chester B. Williams, Massillon Steel Castings Co., Massillon, Ohio (Also V. C.)
- CENTRAL ILLINOIS-Program Chairman Robert E. Dickison, Asst. to Mgr., Brass Foundry Co., Peoria, Ill. (Also V. C.)
- CENTRAL INDIANA—Chairman Allen J. Reid, General Refractories Co., Indianapolis, Ind. Program Chairman Robert Spurgin, III, Swayne Robinson & Co., Richmond, Ind. (Also V. C.)
- CENTRAL MICHIGAN—Chairman Jack F. Secor, Hill & Griffith Co., Niles, Mich., and Secretary Thomas T. Lloyd, Albion Malleable Iron Co., Albion, Mich.
- CENTRAL NEW YORK—Chairman David Dudgeon, Jr., Utica Radiator Co., Utica, N. Y., and Program Chairman Wm. D. Dunn, Oberdorfer Foundries, Syracuse, N. Y. (Also V. C.)
- CENTRAL OHIO-Chairman Daniel E. Krause, Gray Iron Research Institute, Columbus, Ohio, and Treasurer C. Wm. Gilchrist, Cooper-Bessemer Corp., Mt. Vernon, Ohio
- CHESAPEAKE—Chairman A. A. Hochrein, American Smelting & Refining Co., Baltimore, Md., and Program Chairman Allen S. Kittrell, Leach Pattern Shop, Baltimore, Md. (Also V. C.)
- CHICAGO—Chairman C. V. Nass, Pettibone-Mulliken Corp., Chicago, and Program Chairman Walter Moore, Burnside Steel Foundry Co., Chicago (Also V. C.)
- CINCINNATI DISTRICT—Chairman Martin E. Rollman, Cincinnati Milling Machine Co., Cincinnati, and Program Chairman A. W. Schneble, Jr., Advance Foundry Co., Dayton, Ohio

- (Also V. C.)
- DETROIT—Chairman Jess Toth, Harry W. Dietert Co., Detroit, and Program Chairman Vaughan C. Reid, City Pattern Fdy. & Mach. Co., Detroit (Also V. C.)
- EASTERN CANADA—Program Chairman W. Turney Shute, Canadian Car & Foundry Co., Ltd., Westmount, Quebec, Canada (Also V. C), and Secretary A. H. Lewis, Met., Dominion Engineering Works, Ltd., Montreal, Que., Canada
- EASTERN NEW YORK—Chairman Leo Scully, Scully Foundry & Machine Co., Coxsackie, N. Y., and Program Chairman John Waugh General Electric Co. Schenestady, N. Y. (Also V. C.)
- Waugh, General Electric Co., Schenectady, N. Y. (Also V. C.)
 METROPOLITAN—Chairman D. S. Yeomans, G. F. Pettinos, Inc.,
 Philadelphia, and Program Chairman D. Frank O'Connor,
 Pres., O'Connor's Foundry, Inc., Hackettstown, N. J. (Also
 V. C.), and Secretary-Treasurer J. F. Bauer, Hickman, Williams & Co., New York
- MEXICO—Chairman Ing. F. Fernando Gonzalez Vargas, Mexico, D. F., Mexico, and Program Chairman N. S. Covacevich, Mexico, D. F., Mexico (Also Sec.-Treas.)
- MICHIANA-Chairman Wm. Ferrell, Auto Specialties Mfg. Co., St. Joseph, Mich.
- Mo-Kan—Chairman W. Leslie Neville, Neville Foundry Co., Inc., North Kansas City, Mo., and Program Chairman Henry C. Deterding, Sonken Galamba Corp., Kansas City, Kans. (Also V. C.)
- NORTHEASTERN OHIO—Chairman Fred J. Pfarr, Lake City Malleable Co., Cleveland, and Program Chairman Gilbert J. Nock, Nock Fire Brick Co., Cleveland (Also V. C.)
- NORTHERN CALIFORNIA—Chairman John R. Russo, Russo Foundry Equipment Co., Oakland, Calif., and Program Chairman Philip C. Rodger, General Metals Corp., Oakland, Calif. (Also V. C.)
- NORTHERN ILLINOIS-SOUTHERN WISCONSIN-Chairman F. W.

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C. Mso falpert dry alif. W. Thayer, Gunite Foundries Corp., Rockford, Ill., and Program Chairman Carl L. Dahlquist, Greenlee Bros. & Co., Rockford, Ill. (Also V. C.)

NORTHWESTERN PENNSYLVANIA-Chairman F. P. Volgstadt, Griswold Mfg. Co., Erie, Pa., and Program Chairman J. D. James, Cooper-Bessemer Corp., Grove City, Pa. (Also V. C.)

ONTARIO-Program Chairman Reginald Williams, Canadian Westinghouse Co., Ltd., Hamilton, Ont. (Also V. C.)

OREGON-Chairman James T. Brodigan, Columbia Steel Foundry, Portland, Ore., and Program Chairman James T. Dorigan, Electric Steel Foundry, Portland, Ore.

PHILADELPHIA-Program Chairman George Bradshaw, Philadelphia Navy Yard, Philadelphia (Also V. C.)

QUAD CITY-Chairman H. A. Rasmussen, General Pattern Corp., Moline, Ill., and Program Chairman Wm. C. Bell, Frank Foundries Corp., Moline, Ill. (Also V. C.)

ROCHESTER-Chairman K. R. Proud, Anstice Co., Rochester, N. Y., and Program Chairman Chas. E. Vaughan, Ritter Co., Inc., Rochester, N. Y. (Also V. C.)

SAGINAW VALLEY-Chairman Howard H. Wilder, Eaton Mfg. Co., Fdy. Div., Vassar, Mich.

St. Louis District-Chairman John Williamson, M. A. Bell Co., St. Louis, and Program Chairman Ralph M. Hill, East St. Louis Castings Co., East St. Louis, Ill. (Also V. C.)

SOUTHERN CALIFORNIA-Program Chairman Henry W. Howell, Howell Foundry Co., Los Nietos, Calif. (Also V. C.)

TENNESSEE-Chairman P. L. Arnold, U. S. Pipe & Foundry Co., Chattanooga, Tenn., and Program Chairman Porter Warner,

Ir., Porter-Warner Industries, Chattanooga, Tenn. (Also V. C.)

TEXAS-Chaiman W. H. Lyne, III, Hughes Tool Co., Houston, Tex., and Program Chairman John M. Bird, American Brass Foundry, Ft. Worth, Tex. (Also V. C.)

TIMBERLINE-Chairman John W. Horner, Jr., Slack-Horner Brass Co., Denver, Colo., and Secretary James Schmuck, Rotary Steel Casting Co., Denver, Colo.

TOLEDO-Secretary-Treasurer R. C. Van Hellen, Unitcast Corp., Toledo, Ohio

TRI-STATE-Chairman F. E. Fogg, Acme Foundry & Machine Co., Coffeyville, Kan., and Program Chairman D. A. Mitchell, Progressive Brass Mfg. Co., Tulsa, Okla. (Also V. C.)

TWIN CITY-Chairman S. L. Cameron, Jr., Valley Iron Works, Inc., St. Paul, Minn., and Program Chairman C. F. Quest, J. F. Quest Foundry Co., Minneapolis (Also V. C.)

WASHINGTON-Chairman S. H. Marshall, Atlas Fdy. & Machine Co., Tacoma, Wash., and Program Chairman James D. Tracy, Salmon Bay Foundry, Seattle (Also V. C.)
WESTERN MICHIGAN—Chairman S. H. Davis, Campbell, Wyant &

Cannon Co., Muskegon, Mich.

WESTERN NEW YORK-Chairman A. A. Diebold, Atlas Steel Castings Co., Buffalo, N. Y., and Program Chairman Erwin Deutschlander, Worthington Pump & Mach. Corp., Buffalo, N. Y. (Also V. C.)

Wisconsin-Chairman W. W. Edens, Badger Brass & Aluminum Fdy. Co., Milwaukee, Program Chairman A. F. Pfeiffer, Allis-Chalmers Mfg. Co., West Allis, Wis., and Vice-Chairman George E. Tisdale, Zenith Foundry Co., Milwaukee.

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OPERATION OF THE CUPOLA

By W. W. Levi*

ABSTRACT

The author discusses the elements of cupola practice, such as charging equipment; front-slagging cupolas; balanced-blast blast control; moisture content control of blast; mechanical charging equipment; front-slagging cupolas; balanced-blast cupola; inoculants; carbon equivalent; calculating carbon content of the iron.

THE MODERN CUPOLA FURNACE when compared with other types of melting units is truly in a class by itself. Once its operation is clearly understood and the cupola is handled with a reasonable degree of intelligence, it is capable of producing high-quality products at relatively low melting cost.

It differs from other melting units in many respects, one of these being that the fuel and melting stock are in intimate contact with each other during the melting operation. This fact alone means that both physical and chemical properties of the charge are constantly changing during the course of their descent through the stack.

The components of the charge are placed in the cupola at room temperature, but once inside the stack the temperature starts immediately to rise due to absorption of heat from the effluent gases. As the stock descends farther and farther it becomes hotter and hotter until finally it changes from the solid to the liquid state.

After melting, the metal becomes superheated and is then either stored for a time in the well of the cupola to be tapped out later, or it may be tapped out as fast as it is melted as is the case with front-slagging continuous-flow cupolas. Regardless of the tapping technique employed, the molten metal starts immediately to cool off during the time required to accumulate a sufficient amount to fill a particular ladle and continues to do so during the handling and pouring operations. Upon further cooling in the molds it changes back to the solid state and finally reaches room temperature in the form of the rough casting.

It must be remembered while some of the physical changes just mentioned are taking place that the metal is in contact with incandescent coke. In many instances this results in an increase in total carbon, as a large percentage of commercial gray iron castings are made from cupola mixtures which contain less carbon than the final product, the difference being the result of carbon absorption from the fuel. However, it seems likely that in some cases there is a loss in carbon content during melting. This is generally encountered in connection with cupola charges containing high percentages of pig iron melted with relatively high coke ratios. Other chemical changes generally include a sizeable loss or decrease in silicon and manganese and minor increases in sulphur and phosphorus contents.

Continuous Melting in Cupola

Another difference between the cupola and other types of furnaces lies in the fact that the cupola is a continuous melting unit. This means, of course, for all practical purposes that molten metal is available at any time throughout the duration of a heat.

During World War I much was learned regarding cupola operation with the result that rapid progress was made toward improving the quality of the product to its present high level. For a number of years many foundries have regularly produced cupola irons with strengths ranging from 25,000 to 70,000 psi tensile strength, depending upon customers specifications and the particular engineering application. More recently, with the advent of ductile cast iron, it has been possible to produce cupola-melted irons haying tensile strengths well in excess of 100,000 psi with considerable ductility as measured in terms of elongation and reduction of area.

In addition to being capable of producing many grades of commercial gray iron the cupola has been used to melt highly alloyed irons such as 14 to 17 per cent silicon iron used for handling chemicals, 16 per cent nickel, 3.5 per cent chromium, 7 per cent copper iron (Ni-Resist), for heat and corrosion resistance, 5 per cent nickel, 1.5 per cent chromium iron (Ni-Hard), for resistance to abrasive wear, and others containing up to 40 per cent of alloy. For many years the cupola was used as an important unit in the manufacture of 11 to 14 per cent manganese steel. The process consists briefly of "blowing" steel in the Besse-

^{*}Metallurgist, Lynchburg Foundry Co., Radford, Va.

mer converter, transferring the molten steel to the pouring ladles and at this point making an addition of cupola-melted ferromanganese.

Other Uses of Cupola

Copper and bronze have been successfully melted in the cupola and both the Bessemer and open hearth steel mills have installed cupolas to speed up production in locations which are not serviced by blast furnaces. In the past, many malleable iron foundries did all of their melting "cold" in the air furnace. Inasmuch as this is a batch-type furnace it was necessary in many shops to install several furnaces in order to provide molten iron continuously throughout the day or when the demand exceeded the capacity of a single furnace.

In recent years, many malleable foundries have changed from "straight" air furnace melting to the duplex process, using the cupola to melt and superheat the iron and either a reverberatory or electric furnace as the secondary unit where the metal can be held for a period of time in order to make final adjustments in composition. Thus, the cupola has in effect converted the batch-type air furnace into a continuous unit with the resultant saving in capital expenditures and at the same time has reduced melting costs in malleable foundries.

Another interesting application of the use of the cupola is in the manufacture of mineral wool for insulating purposes. For this purpose, unlined, waterjacketed cupolas are used and apparently the most popular size is a stack of either 48-in. or 54-in. I.D. The raw materials used may consist of air-cooled blast furnace slag or natural dolomitic rock (and possibly others), sized from 13/4 in. to 4 in. and high-grade foundry coke. The charge will weigh from 1,500 to 1,750 lb including the coke which is used in a ratio of about 5.5:1. A typical blower installation would be one having a capacity of 2,400 cfm at 20 oz pressure. Temperature of the melt is about 2,600 F and production rate varies from 1 to 1.75 tons per hour. Recent installations, with economy of fuel as the objective, have included cupolas with charging bells to minimize the escape of gases during the charging operation. Under these conditions the cupola gases have been used both for the purpose of preheating the cupola blast to a temperature of 350 to 400 F, and for firing the boilers used to furnish process steam. The water used for cooling the cupola shell is used as boiler feed water.

Cupola A Versatile Melting Unit

From what has been said, it is evident that the cupola is a most versatile melting unit capable of producing a wide variety of products satisfactory for many engineering applications. It is also evident that conditions, within a furnace capable of melting such a wide variety of end products as is the cupola, must of necessity vary within broad limits depending on what it is intended to produce. It is the intent of this paper to discuss only those variables effecting the operation of cupolas producing commercial gray iron castings.

The subject of cupola operation and control is, to

say the least, a most controversial one and, in spite of the fact that there are almost as many cupola procedures as there are operators, results are, for the most part, quite satisfactory. Regardless of the procedure followed, cupola operators have several objectives in common. Among these is the delivery of good hot iron from the very beginning of the heat, and the rate of delivery to the pouring stations must be equal to the demands of the molding department. Metal composition must be suitable for the various section sizes involved and should be processed and inoculated so as to develop the best possible mechanical properties and highest quality commensurate with the type of castings being produced.

Cupola Bed Practice

While the matter of cupola bed practice may be considered rather elementary at this time it plays an important part in helping obtain hot iron at the start and throughout the duration of the heat. For this reason it will be discussed briefly. In one foundry where the writer has had experience, the kindling wood and practically all of the bed coke are put into the cupola at one time. About 6 hr before "wind-time" the bed is "lit-off" and allowed to burn through very slowly. Before any metal is charged the bed is levelled off with cold coke, and charging then proceeds until the cupola is full.

In another foundry the bed coke is put into the cupola in three equal portions at 2-hr intervals, while in a third department about one-half of the bed is "lit-off" only about 4 hr ahead of "wind-time," and here it is necessary to use a gentle blast to thoroughly ignite the coke. All of these procedures are satisfactory, and will produce hot iron at the start of the heat if it is borne in mind that the coke which occupies the cupola well is somewhat more difficult to ignite than the portion of the bed above the tuyeres. It is therefore important to use a generous amount of kindling material unless kindling tuyeres or some other means of lighting off are used.

Recently an electric coke igniter has appeared on the market for lighting off the cupola bed. With the use of this equipment it is claimed that no wood, oil, or gas are required and results obtained in both large and small cupolas are reported to be quite satisfactory.

The longer burning in time is preferable, as this results in better preheating of the cupola lining and well, and when cupolas are operated on alternate days ample time is available. However, when it is necessary to use the same cupola every day there usually is less time available (after completion of the daily "patching" job) between light-off-time and wind-time.

In order to provide a large volume of fluid slag as soon after the start of the heat as possible, two extra charges of limestone are placed on the bed just before charging starts, and 4 lb of fused soda ash per ton of metal are used on each charge used to fill the cupola in addition to the regular charge of limestone. The layer of slag thus formed acts as a protective covering over the molten iron in the cupola heat, resulting in considerably higher metal temperatures at the start of the heat. This may be due to the fact that the slag blanket protects the molten iron from the cooling

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effect of the incoming blast, or heat losses through radiation from the surface of the molten metal are reduced.

One of the requirements for successful cupola operation is an exact knowledge of the composition and behavior of the raw materials used. This involves chiefly the metallic components of the cupola charge and the metallurgical coke or cokes on hand or available. Needless to say, the limestone, dolomite, or whatever fluxing material is used, should be of the best quality.

Each carload of pig iron should be checked against the shippers' analysis and the user should make determinations for total carbon with the same care and accuracy used in checking silicon, sulphur, manganese, and phosphorus. The composition of return scrap and gates is obtained from daily laboratory records of analysis. The analysis of a carload of purchased cast iron scrap would be an almost endless task and would be of very little value after it was completed. Nevertheless, this should not discourage the use of large percentages of cast scrap in the cupola charge, as the composition of the various types of scrap available is known quite definitely. However, the party responsible for the purchasing of scrap should insist that any given carload contain only one type of scrap and not a mixture of several kinds. Each carload should be rigidly inspected, both upon arrival in the iron yard and during the course of unloading to the storage piles or to the charging cars or buckets.

After a brief period of training in the recognition of various types of scrap, crane operators and charging crews can offer invaluable assistance to the cupola foreman, as they see practically every piece of iron used during the course of operation.

If the calculation of the composition of the ingoing charge is to be of any value, we must insist that all components of the charge be carefully weighed. Weighing equipment must be checked frequently by qualified scale repair men and kept in good order. Too frequently, when the analysis "gets off," investigation reveals that the iron yard scale or some other scale is

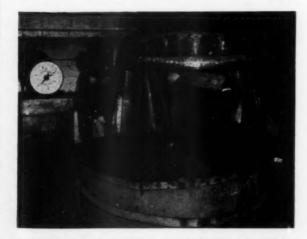


Fig. 1-Track scales for weighing heavy loads; charge being made up with crane and magnet.



Fig. 2—Platform scale of 500-lb capacity being used to weigh spiegeleisen.

out of order and the charge has been estimated. The weighing equipment must be rugged and accurate, sensitive over the range in which it is to be used, and intelligently chosen for the job it is intended to do. Generally speaking, one set of scales will not be satisfactory for weighing all of the materials entering into the charge. For example, 50 lb of 50 per cent lump ferrosilicon ordinarily cannot be weighed accurately on a set of scales designed for heavy loads where the smallest division on the dial represents 25 lb and the tare weight of the charging car and bucket may be 3,000 or 4,000 lb. Iron yard scales of this type are shown in Fig. 1, where charge is being made up with crane and magnet.

Assuming that the scaleman works to an accuracy of plus or minus one division on the scale dial, his error could then be plus or minus 25 lb. This represents 12.5 lb of silicon, which in turn means a possible variation of 0.50 per cent silicon in a 2,000-lb charge This is not a hypothetical case as these figures repre sent a set of conditions which actually exist in a plant For weighing materials used in quantities up to abou. 50 lb an ordinary spring scale has been found to be quite satisfactory. The importance of keeping scales in good working order cannot be over-emphasized. One good procedure consists of engaging the services of a professional scale repair man to make a monthly check and inspection of all weighing equipment. In addition to this all scales should be checked daily with standard scale weights.

For weighing "concentrated" materials such as silvery pig iron and spiegeleisen in quantities of 50 to

150 lb a portable platform scale of 500-lb capacity is recommended. A scale of this sort is shown in Fig. 2. Coke is weighed on the same type of scales and the equipment need not be expensive or elaborate. Containers used for coke may be scrap steel drums cut in two with handles welded to each half. The gross weight is marked on each container and, for convenience and ease of handling, the coke is weighed in batches of 100 lb or less. Plant layout and conditions will determine, at least to some extent, the type of weighing equipment to be used. This subject should be given considerable thought and attention by top management as well as the cupola foreman as there simply is no substitute for accurate weighing.

Figure 3 shows pendant type monorail scales in use. With this equipment the scales and charging buckets can be moved to any storage bin in the iron yard where the components of the charge are loaded by hand. Hand loading is highly desirable when uniformity of composition through accurate weighing is essential as is the case when producing automotive

irons.

Makeup of Cupola Charges

When making up cupola charges consideration should be given to the sequence followed in placing the components in the charging buckets. A definite sequence makes for a more uniform operation and this writer feels that it is important to obtain maximum carbon pick-up commensurate with the type of charge being used. This applies to hypoeutectic irons of the automotive types as well as to fairly strong hypereutectic irons used for production of castings in water-cooled metal molds. Best results have been obtained by placing the portion of the cupola charge containing the lowest percentage of carbon next to the coke charge and the portion containing the highest percentage of carbon furthest from the coke charge. Components containing intermediate percentages of carbon are placed between the first two components mentioned. The steel will be the component containing the least carbon and the pig iron



Fig. 3-Pendant-type monorail scales can be moved to various storage bins where charges are made up by hand.

will probably contain the most. The intermediate components will be returns, home scrap, and purchased scrap.

Air used in the cupola melting process should be looked upon as an extremely important raw material. When melting 200 tons of iron in a cupola, about 5,200,000 cu ft of air will be required. Air at 14.7 psi atmospheric pressure and 60 F weighs 0.0764 lb per cu ft. By means of simple arithmetic it is then evident that for every pound of iron melted, practically 1 lb of air is required. Emphasis has been placed on importance of accurate weighing of the metal, coke, limestone, and other materials comprising a cupola mixture. However to insure uniformity of operations it is just as important that the raw material, air, used in quantities nearly equal in weight to the tonnage of iron melted be metered or weighed with the same care and accuracy as the other components of the charge. For this purpose, the company with which the author is associated has installed constant-air-weight-control blowers in two of its foundries. One of these is capable of delivering 13,000 cu ft of air per minute at a maximum pressure of 2 psi and the other will deliver 14,500 cu ft per minute at a pressure 2.5 psi. The amount of air delivered to the cupolas served by these blowers is measured in pounds per minute so that the meter reading will closely approximate the cupola melting rate in pounds per minute.

The older blowers are of the positive displacement type. Each revolution delivers a definite volume of air to the cupolas, so that running at a constant number of revolutions per minute will deliver a fairly constant volume of air. However, to deliver a constant weight of air to the cupolas with the older blowers would require constant adjustment in speed corresponding to the slightest changes in atmospheric temperature and pressure. For this reason it is obvious that the constant-air-weight-control blower eliminates the variables resulting from constantly changing at-

mospheric conditions.

Cupola Air Blast Moisture Control

Another variable of considerable magnitude which effects cupola operations is the moisture content of the air blast. This may vary from 2 grains per cubic foot on cold winter days to as much as 12 grains per cubic foot on hot sultry days in the summer time. The moisture content of the air can also vary within the limits referred to during the course of a single day's melting operation. There are 7,000 grains in a pound. Now let us consider the possible variation in the amount of water carried into a cupola with the air blast when melting 200 tons of iron per day. This operation, as previously stated, will require about 5,200,000 cu ft of air, so that the water blown into the cupola will be between 1,500 lb (180 gal) and 9,000 lb (1,080 gal). From these figures it is evident that the control or elimination of a variable of such magnitude is well worth consideration. For this reason equipment for controlling moisture in the cupola blast has been installed in a number of plants.

One such installation is known as a "Kathabar system." It is designed to remove moisture from 13,000 cu ft of air per minute down to 4 grains per cubic

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foot in the summer time and is also capable, in dry weather, of introducing as much as 7 grains per cubic foot. The principle of operation lies in the fact that the system contains a solution of lithium chloride, known as kathene, which under the proper conditions has a large capacity for absorption of moisture from the air. However, this absorption capacity can be controlled and predetermined by regulating the temperature of the kathene. Moist air from the outside is taken into the system through a filter to remove dust and dirt and then passes through "contactor cells" in intimate contact with a spray of kathene where moisture is removed (or added) to a predetermined level within the design limits of the system. To prevent loss of kathene due to carry-over, the air passes next through a series of glass wool eliminators and is finally drawn through a set of heating coils and thence to the cupola blower intake. The heating coils maintain the cupola blast at a temperature of 105 F.

When a cupola is operating so as to require 10,000 cu ft of air per minute taken into the Kathabar system with 8 grains of moisture per cubic foot and leaving with only 4 grains per cubic foot, the kathene solution is taking on about 40 gal of water per hour. To maintain efficient operation of the system this excess water must be removed continuously and as rapidly as it is absorbed. This is accomplished in the "regenerator" unit where a part of the diluted kathene, after being heated to a temperature of 235 F in the kathene heater, is sprayed over a relatively small contactor surface where a small stream of outside air passes over it. At the elevated temperature (235 F) referred to, the kathene gives up the moisture which was previously removed from the cupola blast. The lower part of the regenerator unit serves as a storage space for the kathene solution when the system is not in operation and it also houses a series of filters which constantly remove foreign material from the solution when the system is in operation.

In winter time it is necessary to add moisture to the cupola blast in order to maintain a year-round constant control. This is accomplished by means of a small heating coil known as the "humidifying heater" where the solution is heated with steam to a temperature at which it will give up moisture to the cupola blast in just the required amount to maintain proper conrol. The principle of operation of the "humidifying heater" is somewhat the same as that of the regenerator.

While the above discussion has dealt with a moisture control system designed to maintain a year-round uniform moisture content of 4 grains per cubic foot of cupola air blast, this figure in itself is not particularly significant. Viewed strictly from the standpoint of uniformity through elimination of variables it would be unimportant whether the system was designed to maintain a year-round moisture content of 2 grains per cubic foot or 5 grains per cubic foot or any other figure just so long as the variation in moisture content of the blast is eliminated. However, a recent exhaustive study of this subject by the Gray Iron Research Institute indicates that there are definite advantages to be gained by operating the cupola with as "dry" a

blast as is practical, bearing in mind that the cost of this type of equipment rises rapidly as the moisture content of the air blast is reduced. The work by the Gray Iron Research Institute was in connection with a 10-in. I.D. cupola, operated with rear slagging and conventional intermittent tapping. All iron charges weighed 30 lb and a good quality by-product coke in the ratio of 7.5:1 was used for melting. The moisture content of the cupola blast was deliberately varied over a wide range by proper manipulation of a Kathabar system installed for this purpose. Variations in all other phases of the operation were held to a minimum. A summary of the results of several heats is given in Table 1.

TABLE 1—EFFECT OF MOISTURE IN AIR BLAST ON TEM-PERATURE AND CHEMICAL COMPOSITION OF CUPOLA IRON*

| Moisture In Blast | Iron Temp. | | Chemical | Analysis, | Per Cent | ent |
|----------------------|---------------|------|----------|-----------|----------|-------|
| Gr/Cu Ft | F | Si | S | Mn | P | T. C. |
| 1.8 | 2805 | 1.89 | 0.11 | 0.81 | 0.13 | 3.45 |
| 2.4 | 2795 | 1.84 | 0.11 | 0.80 | 0.14 | 3.41 |
| 7.5 | 2755 | 1.79 | 0.12 | 0.79 | 0.13 | 3.30 |
| 10.3 | 2700 | 1.80 | 0.11 | 0.77 | 0.14 | 3.24 |
| 14.3 | 2670 | 1.72 | 0.13 | 0.73 | 0.13 | 3.17 |

• (Presented by permission of Gray Iron Research Institute.)

The Gray Iron Research Institute states, "The variation in moisture shown in Table 1 is about the same as would be obtained in winter on a cold, dry day and in summer on a hot, humid day."

Effects of Blast Moisture

The above data indicate at least three significant effects of increasing the moisture content of the cupola blast. They are, lower temperature of the iron at the cupola spout, lower percentage of total carbon in the iron at the cupola spout, and increased loss of silicon during the melting operation. (There is also an increase in loss of manganese during melting as the moisture content of the cupola blast increases, but this loss is rather insignificant.) For this discussion, only the effects of the maximum variation in moisture content of the cupola blast will be considered. As indicated, increasing the moisture from 1.8 to 14.3 grains per cubic foot results in a drop in temperature from 2805 F to 2670 F or a loss of 135 F. Such a temperature difference is of real importance and in connection with the production of some types of castings might mean the difference between success and failure. Loss in metal temperature can, of course, be taken care of by increasing the amount of coke between charges, but if a condition corresponding to 1.8 grains of moisture per cubic foot of air blast could be maintained at all times a considerable reduction in coke consumption could be made. One of the indirect effects of increasing moisture content of the cupola blast from 1.8 to 14.3 grains per cubic foot is a drop in total carbon content from 3.45 to 3.17 per cent or a reduction of 0.28 per cent T. C.

Inasmuch as it is felt that carbon pickup, all other things being equal, is proportional to melting tem-

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perature the direct cause of this drop in total carbon content is probably due to the reduction in melting temperature noted above. To compensate for a change in total carbon content of this magnitude would require quite a drastic change in the make-up of the cupola charge as it is practically impossible to increase the carbon content after the iron has been melted. The tabulation further indicates that iron melted with "dry" blast (1.8 grains per cubic foot) contains 0.17 per cent more silicon at the cupola spout than the same charge melted with "wet" blast (14.3 grains per cubic foot). This loss of silicon can be easily compensated for by an adjustment in the cupola charge or by making a late addition of ferrosilicon. However, silicon is a relatively expensive commodity and therefore any reduction in the amount lost during the melting process will reduce operating costs.

The preceding paragraphs dealing with the "Kathabar system" actually describe a means of moisture control based on the affinity of lithium chloride solution for water vapor. Other systems which cool the air by refrigeration remove moisture from the cupola blast mechanically by "freezing it out" and this type of equipment is used successfully in a number of foundries. It should be borne in mind that controlling the moisture content of the cupola blast is a refinement which will be lost sight of unless all other phases of the operation are carefully supervised and controlled. Moisture control equipment is quite expensive and the value of an installation can be obscured or entirely lost sight of if an attempt is made to use it in connection with poor cupola practice.

Balanced-Blast Type Cupola

The balanced-blast type of cupola helps maintain good uniform conditions within the cupola from start to finish of the heat and this feature is helpful and of considerable importance when the heat is of long duration. Five of the seven cupolas operated by one company are of the balanced-blast type, each equipped with one row of main tuyeres and two rows of auxiliary tuyeres. The main tuyeres are provided with slide valves so that they may be opened or closed at will during the course of the heat. Two of the main tuyeres diametrically opposite are closed at the same time so that the area directly in front of them is heated to a temperature sufficiently high to melt off all overhanging slag. After 8 or 10 min these are opened and two others are closed. This procedure is continued throughout the heat, and results in the prevention of bridging and blocking-up of the main tuyere openings. The openings of the auxiliary tuyeres are adjustable, but the settings cannot be changed without shutting off the cupola blast. The upper, or auxiliary tuyeres, bring about more complete combustion of the coke and thereby make possible a more efficient cupola operation, and in conjunction with the controllable main tuyeres the balanced-heat cupolas melt somewhat faster and at higher temperatures with the same coke ratios than the cupolas provided with conventional tuyeres.

Auxiliary cupola equipment plays an important part in control and uniformity of the molten iron. All

cupolas in the same company's foundries are charged mechanically. Mechanical charging usually improves the operation because it is easier to keep the cupola filled to the charging door level at all times during the melting operation, and the types of charging equipment used distribute both the metal and coke in the cupolas in a much more satisfactory manner than is accomplished by hand. This is especially true with cupolas of larger inside diameters, where charging crews often are prone to place the heavier components of the charge near the charging door opening. In hot weather more of the coke charge than should be is placed near the charging door in order to protect the "chargers" from heat inside the cupola.

In its foundries, this company has two distinctly different types of mechanical charging equipment. One type uses cylindrical charging buckets of the drop-bottom type. The two bottom doors are semicircular in shape and are hinged at the center along a diameter of the bucket. When the bucket and contents are inside the cupola the charge is released by tripping the latch mechanism used to hold the bottom doors in place. This type of charging tends to place the melting stock in the cupola in the form of two small mounds diametrically opposite. In order to keep the charges in the cupola level, alternate buckets are turned 90 deg from the preceding one, thus filling up the valleys between the mounds just referred to. By careful supervision satisfactory results are obtained with this equipment. The other type of equipment consists of charging buckets provided with coneshaped bottoms. When the bottom is lowered the charge is distributed uniformly along the periphery of the cupola lining.

When the change from hand charging to mechanical charging with the cone-bottom buckets was made an increase in temperature of the iron at the cupola spout was immediately apparent, all other things remaining the same. One explanation for this improvement lies in the fact that the cone-bottom buckets distribute the charges in a manner which results in less packing or lower density of the stock near the center of the cupola, which in turn permits better blast penetration and more uniform combustion of the fuel over the entire cross-section of the cupola. On the other hand, some authorities feel that a large amount of the cupola blast, following the line of least resistance, passes out of the stack up along the refractory lining. By distributing the charges in the manner achieved with the cone-bottom buckets a large portion of the coke, as previously stated, slides to the cupola lining. It is here that it comes into contact with the air blast and consequently a relatively high thermal efficiency is obtained.

Front-Slagging Cupolas

In view of the growing interest in front-slagging cupolas, a brief discussion of this type of operation will be given here. Before any operator can consider front slagging, the cupola must be checked to see that the tuyeres are high enough above the sand bottom so that when the air supply is reduced or shut off during the course of the heat the metal which backs into the cupola does not raise the slag level inside to a height

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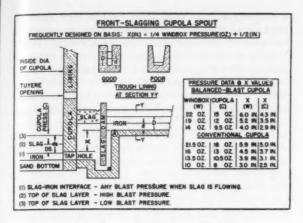


Fig. 4-Front-slagging cupola spout.

where slag will run into the tuyeres. Furthermore, there is a greater accumulation of slag inside the cupola when operating at low blast pressures than when operating at high blast pressures and provision must be made for the storage of this relatively large amount of slag. The design of the front slagging spout is that of a simple U-tube in which the air pressure plus the downward pressure of molten slag inside the cupola are balanced against a head of molten slag and iron outside the cupola. Figure 4 is a schematic drawing showing the construction of a front-slagging spout with a series of lines showing levels of slag and iron both inside and outside the cupola under various operating conditions. In constructing the spout, the bottom of the trough on the side of the slag dam away from the cupola should be practically level for a distance of about 6 to 18 in. starting at location A. In the drawing, the spout at location A is built up above the top of the tap hole a distance designated as X. It should be pointed out here that the dimension X should be kept as small as possible in order to insure a flow of slag from the cupola soon after tapping out.

Obviously X must not be so small that there is leakage of air through the tap hole, but it should be held down so that slag will flow from the cupola under conditions involving as wide a range of windbox pressures as may be encountered with any particular cupola operation. In the first edition of HANDBOOK OF CUPOLA OPERATION, on pp. 44 and 45 in Fig. 22 and 23 respectively, there are shown tabulations for the value of X in inches corresponding to various cupola windbox pressures. It seems probable in making calculations for X that iron has been assigned a value of 0.26 lb (about 1/4 lb) per cubic inch and on this basis a head of 1 in. of iron would balance 4 oz (1/4 lb) of windbox pressure. The 1/2 in. added to X in each case is apparently to provide a positive seal against air leakage through the tap hole. Based on the values assigned to X in the tabulations just referred to it is evident that all conditions can be covered by the equation:

X (in.) = 1/4 Windbox Pressure (Oz) + 1/2 in.

Calculations made in accordance with the above equation are simple and convenient and the results

obtained may be considered rough approximations for the value of X to be used in constructing frontslagging spouts. However, actual practice indicates that X can be considerably less than values obtained by the use of the equation. There are several reasons for this. First, the equation implies that the U-tube previously mentioned is balanced on the outside of the cupola by a column of iron X inches high. Actually this height is always greater than X by an amount equal to the depth of the iron flowing in the cupola trough at its highest point (designated as A in Fig. 5) on the side of the slag dam away from the cupola. This depth has been indicated as "d" under conditions of slow melting with low blast pressure and as "D" when melting at a high rate with relatively high blast pressure. By keeping the passage for iron in the trough narrow at Section Y-Y in the manner labelled "Good," in Fig. 5 the depth of molten iron at location A will be appreciably effected by variations in melting rate. The depth will be proportional to both melting rate and wind pressure inside the cupola and will thus provide a partially compensating automatic control or variation in the value of X. For example, under actual operating conditions with the trough on a 72-in. cupola lined in the manner labelled "Good" (Fig. 5) the depth of iron at point A was found to be 11/4 in. (depth "d") when the windbox pressure was 8 oz. When the windbox pressure was raised to 17 oz the depth of iron at point A was found to be 21/4 in. (depth "D"). Thus the increase in depth of the stream of iron by 1 in. has automatically compensated for 4 of the 9 oz increase in windbox pressure. A wide, flat-bottomed lining in the trough at section Y-Y shown as "Poor" method should be avoided first, because this necessitates building the trough with dimension X greater than when it is lined in accordance with the "Good" method and second, because variations in wind pressure and melting rate have practically no effect on the depth of molten iron in the trough at location A. As will be shown later any unnecessary addition to the dimension X will result in an excessive accumulation of slag inside



Fig. 5—Stationary-type cylindrical forehearth of 3,200-lb capacity; fused soda ash slag flowing from top of iron in the forehearth.

the cupola with the attendant danger of the slag running into the tuyeres. This situation can be especially serious when operating at low blast pressures.

Molten Cupola Slag Layer

Another item which has not been taken into consideration in the conventional method of calculating X is the depth of the layer of molten cupola slag over the iron between the taphole and the slag dam. However, this layer of slag definitely exerts a force tending to prevent leakage of air through the tap-hole and its effect should be taken into account. Molten slag has been found to have a density of about one-third of that of molten iron and consequently a layer of slag 2 or 3 in. deep could be counted upon to balance approximately 2 to 4 oz of wind pressure. It so happens that the layer of molten slag between the tap hole and the slag dam is shallowest when operating at high blast pressures and deepest when operating at low blast pressures. In other words, the effectiveness of this layer of slag against leakage of air through the tap hole diminishes as the blast pressure increases. As previously stated experience has indicated that slag will flow from a front-slagging cupola even when operated over a wide range of windbox pressures.

In cases where windbox pressure is reduced, flow of slag will temporarily cease but will start again as soon as the head of molten slag which accumulates inside the cupola (under conditions of reduced windbox pressure) plus the reduced windbox pressure are sufficiently great to force the slag out. It is felt that the depth of molten slag inside the cupola is inversely proportional to windbox pressure. This is shown diagrammatically in Fig. 5 where the horizontal line (1), inside the cupola, represents the slag-iron interface when slag is flowing from the cupola regardless of blast pressure. Attention is called to the fact that this interface is below the top of the tap hole. Line (2) represents the top of the slag layer inside the cupola under conditions of high blast pressure, and the depth of this layer has been assigned the value "DS" corresponding with the depth "D" of iron in the cupola spout under the conditions in question. When the blast pressure is reduced the level of line (1) rises immediately and the flow of slag stops. At the same time slag starts to accumulate inside the cupola and continues to do so until it attains the depth "ds" corresponding to the level of line (3). Finally the combined pressure exerted by the increased head of molten slag plus the reduced blast pressure is sufficiently great to force the slag-iron interface back down to its original level, line (1), and under these conditions slags again starts to flow from the cupola.

Cupola Windbox Pressure

Attention is again called to the fact that the usual calculation for X is based on cupola windbox pressure. It has just been shown that the equation for calculating X given above involves some inconsistencies and one of these results from the use of the windbox pressure reading itself. The fact that air flows from the windbox to the inside of the cupola is definite proof that the pressure inside is lower than in the windbox, otherwise there could be no flow of air.

Experimental data indicate in cupolas with multiple rows of tuyeres such as balanced-blast cupolas that the greatest pressure inside the cupola exists in the vicinity of the tuyeres nearest the bottom of the stack and becomes progressively lower at locations further from the bottom. Inasmuch as there is little if any flow of air through the sand bottom and bottom doors after melting is underway the lower part of the cupola can be considered a "dead end" and consequently the pressure inside the cupola at this level will be equal to the pressure inside the cupola at the level of the lowest row of tuyeres. This then is the pressure reading to use for calculating X. It has been found that the pressure differential between windbox and inside of the cupola is proportional to tuyere ratio. In connection with Fig. 5 there is presented a tabulation of "Pressure Data and X Values." The data were obtained in connection with both 72-in. balanced-blast and 72-in. conventional type cupolas. The column headed "Windbox (W)" indicates actual windbox pressure in ounces and the column headed "Cupola (C)" indicates the corresponding wind pressure in ounces inside the cupola. Two pressure gages were used to obtain the various readings. One was attached to the windbox and the other was attached to the cover of one tuyere in the same vertical plane.

In connection with the balanced-blast cupola simultaneous readings over a range of pressures were taken with the slide valve in the tuyere closed. In this manner the pressure recorded at the tuyere was actually pressure within the cupola as there could be no flow of air through the tuyere from the windbox. In connection with the conventional type cupola a plate was used inside the windbox to stop the flow of air through one of the tuyeres. This plate was attached to a shaft or handle passing through the top of the windbox and by this means could be raised or lowered at will. Columns headed "X (W)" and "X (C)" show calculated values of X based on windbox pressures and actual pressures inside the cupola respectively. It will be noted that there is a greater difference between windbox pressure and actual pressure inside, in the case of the balanced-blast cupola than in the case of the conventional cupola. This is because the balancedblast cupola has a higher tuyere ratio. All of the above points to the fact that the dimension X can be considerably less than indicated by the conventional calculation and as previously stated keeping X as small as possible is highly desirable under conditions necessitating variations in windbox pressure during the course of a single day's operation. Table 2 covers

TABLE No. 2

| Bot To Cupola | Distanc | ce X, In | | | | |
|---------------------|----------------|--------------------------------|-----------------------|------------------------|--------|--------|
| | uyeres, in. | Hourly Melting Rate, Max | Tap Hole Size, in. | Windbox Press., oz. | Actual | Calcu- |
| 48 | 29 | 12 Tons | 1.25 D | 22-28 | 41/2 | 63/4 |
| 60 | 36 | 16 Tons | 1.75 D | 6-30 | 33/8 | 5 |
| 72 | 32 | 30 Tons | 2.5 X 2 | 12-30 | 31/2 | 53/4 |

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some of the pertinent data regarding the successful operation of three sizes of front-slagging, balancedblast cupolas and includes distance from bottom plate to center line of main tuyeres, melting rates, tap-hole sizes, range of windbox pressures, and values of X actually in use, and those obtained by calculation.

The distance X for the cupolas in the above tabulation is considerably less than would be found by actual calculation using average windbox pressures. In view of the above it is evident, based on the number of variables involved, that an actual calculation for the value of X would be complicated. The simplest way to determine this dimension is by "trial and error" making sure that provision has been made for reducing X quickly in case slag does not flow from the cupola in a reasonably short time after the start of the heat.

All of the cupolas operated by the company are of the front-slagging type and this sort of operation lends itself well to "processing" of the iron. Molten iron from the cupolas runs directly into forehearths or teapot ladles, where an amount equivalent to several cupola charges is stored and mixed, resulting in greater uniformity of metal composition than would be obtained when tapping directly from the cupola into the pouring ladles. This is especially true when the capacity of the pouring ladles is small (1,000 lb or The size of the forehearth ladle is such that it will hold as much molten iron as the cupola melts in 10 to 15 min. From this it is obvious that different sized forehearth ladles are used with different sized cupolas.

Fused Soda Ash Treatment

While the forehearth is being filled with molten iron and after each ladle is taken out, the metal (in the forehearth or teapot ladle) is treated with fused soda ash for the purpose of reducing the sulphur content, removing entrained silicates, increasing fluidity, and for deoxidation and grain refinement. Figure 5 shows a stationary type cylindrical forehearth of 3,200lb capacity used in connection with a 48-in. balancedblast cupola. In this view, fused soda-ash slag is flowing from the top of the molten iron in the forehearth. Iron is being tapped at a location about 90 deg from the point where the slag is "skimmed off."

While the metal is being transferred from the forehearth or teapot ladle to the pouring ladles it is inoculated with either 75 per cent grade ferrosilicon or zirconium silicide or both. All of the mixtures are calculated with the silicon content in the iron at the cupola spout somewhat (0.20 to 0.40 per cent) below that desired in the final product, thus deliberately leaving room for a "late" addition of ferrosilicon. This type of operation has a distinct advantage in that it makes it possible to pour castings varying widely in section size from the same "base" iron. This is done by adjusting the cupola mixtures to "fit" the castings having the heavier metal sections. For lighter castings varying amounts of ferrosilicon are added, and for the heaviest castings very little ferrosilicon and varying amounts of ferrochromium or one of the stabilizing types of inoculants may be added. In addition to those just referred to, other alloys and inocu-

lants are added to the molten iron when necessary. These may be either the graphitizing type or the stabilizing type. When producing high-strength irons for use in thin-walled castings, double inoculation often is resorted to, the purpose being to facilitate machining. Double inoculation consists in making additions of both graphitizing and stabilizing types, and has been a successful procedure especially in cases where low-carbon-equivalent irons are used.

Use of Inoculants

All of the ordinary alloying elements such as nickel, chromium, molybdenum, copper, titanium and vanadium may be used separately or in combination, depending upon the class of iron to be produced, the particular application, or the customer's specifications. The purpose of the use of inoculants is to control the structure of the iron in the castings, that is, to produce irons having a pearlitic matrix, free of ferrite, with graphite flakes in random distribution.

From each ladle of iron a chill-test specimen is poured which is used as a guide and quick checkup for the cupola operator. The size, or dimensions, of the chill-test specimen will depend upon the composition of the metal, and this in turn is governed by the size and type of castings to be poured. All chill-test specimens are poured against water-cooled, copper chill blocks so that chill depth is not affected by variations in the temperature of the chill block.

As a further aid to the cupola foreman, strict laboratory control is maintained at all times. When the cupolas melt one type or "grade" of iron for several hours in succession, samples for complete chemical

analysis go to the laboratory hourly.

Results of total carbon determinations are returned to the cupola foreman within 10 min and the silicon, sulphur, manganese, and phosphorus are reported as soon as they have been completed. This usually requires 45 min to 1 hr. Two sets of test bars (2 per set) are poured daily from each type of "regular" iron.

When producing high-strength irons, certain irons made to customers' specifications, and practically all large special castings, test bars and samples for laboratory analysis are poured from each ladle.

Carbon Control

The foregoing portion of this paper has dealt in part with some of the variables effecting cupola operations. The remainder will be devoted principally to carbon control and to the development of an equation, based on a knowledge of these variables, for calculating percentage of carbon to be expected in the iron at the cupola spout. Good "carbon control" is one of the prerequisites for "quality control" and, in the opinion of the writer, these terms are practically synonymous, although the importance of the balance of the chemical composition must not be overlooked.

The amount of carbon in cast iron has more influence on the physical and mechanical properties of the castings than any of the other ordinary elements entering into the chemistry of the iron. (Alloying elements and inoculants bring about certain changes due in part to their influence on the amount of graphite

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or combined carbon in the iron and, of course, in part to the effect resulting from being taken into solution by the matrix.)

One of the accepted equations for the calculation of carbon equivalent (C. E. = % T. C. + 0.3 [%Si + %P]) demonstrates clearly the importance attached to the percentage of total carbon in the iron. It means that the percentage of total carbon exerts about three times as much effect on the properties of the iron as do either silicon or phosphorus. In other words, the foregoing equation "indicates that a change in carbon necessitates an inverse change in silicon and/or phosphorus of approximately three times the magnitude" if we are to maintain the same structure and hardness in castings having equal cooling rates.

Furthermore, it is well known that the silicon content of our irons can be increased when necessary by the addition of ferrosilicon after the iron has been melted. Conversely, for some application an excess of silicon (as determined by chill test) can be compensated for by the addition of some sort of stabilizer such as ferrochromium.

S::lphur can be increased or decreased after the metal has been melted. Manganese can be increased and likewise phosphorus if desired. In contrast with the elements just mentioned, it is practically impossible to increase or decrease the carbon content of the iron after it has been tapped from the cupola. This applies particularly to the composition range covering the bulk of our commercial gray iron castings. Furthermore, in certain applications, such as permanent mold irons, a deficiency in total carbon cannot be satisfactorily compensated for by an increase in silicon.

An excess of total carbon in these applications cannot be taken care of quickly as we have no way of reducing the carbon content; the silicon cannot be reduced and, in some of the irons in question, the addition of stabilizing alloys or inoculants is out of the question due to the detrimental effects on the finished product. In view of the foregoing it is obvious that we must make every effort to control the total carbon in the iron at the cupola spout so that it will be in the range most suitable for the castings being produced.

For the purpose of carbon control, six different types of fuel have been used. These are by-product coke, beehive coke, pitch coke, graphite electrodes,

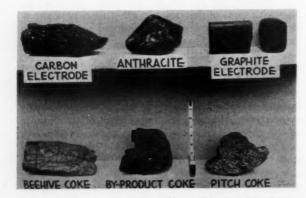


Fig. 6-Various types of fuel used in cupola melting.



Fig. 7-Large 452-lb piece of carbon electrode, asreceived.

carbon electrodes, and anthracite (coal). These are shown in Fig. 6, 7, 8, and 9. The approximate compositions of these materials are given in Table 3.

TABLE 3-ANALYSES OF CUPOLA FUELS

| | By-Product Coke | Beehive Coke | Pitch | raphite Elec- trodes | Elec | - thra- |
|-----------------------|--------------------|-----------------|----------|----------------------------|------|---------|
| Fixed Carbon, % | 88.0-92.0 | 93.0-95.0 | 98.0 Plu | 99 | 92 | 82_85 |
| Ash, % | 7.0-10.0 | 5.0 | 0.5 | 0.04-1.5 | 8.0 | 8.0 |
| Sulphur, % | 0.6 | 0.5 | 0.3 | - | 0.4 | 0.4-0.7 |
| Volatile Matter, % | 0.6-0.9 | 0.9 | 1.0 | _ | 0.4 | 5.0 |

The coke type fuels may be used singly, in combination with other cokes, in combination with anthracite, or in combination with either one of the electrode types of fuel. To date no attempt has been made to melt by means of electrodes or anthracite alone.

All other things being equal, the total carbon in irons melted with by-product coke is lower than that obtained when using either of the other types, and the pitch coke produces irons higher in total carbon than either of the other cokes, while melting with beehive coke produces irons of intermediate total carbon content.

From this it is obvious that there is an infinite number of possible fuel combinations which can be used to help control the total carbon. As an example of the effect of different types of coke on carbon pickup, the comparison between the beehive and by-product types is shown in Table 4.

With mixture "A" the carbon pickup was 1.32 per cent, which is the difference between the average total carbon in the melted iron (3.04 per cent) and the calculated carbon in the charge (1.72 per cent). With mixture "B" the carbon pickup was 1.04 per cent, which is the difference between 3.05 per cent in the

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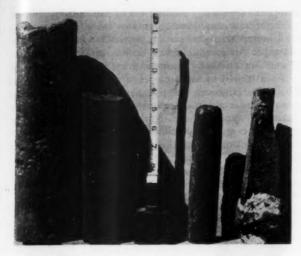


Fig. 8-Variety of pieces of graphite electrodes, asreceived. Note fine-grained fracture of piece in center.

melted iron and 2.01 per cent calculated to be present in the charge.

The carbon pickup with mixture "A" may be looked upon as unusually high (as compared to results obtained with mixture "B") in asmuch as it contained 25 per cent more steel and 18.1 per cent less automotive cast scrap calculated as 3.20 per cent total carbon than mixture "B" (these percentages being based on the amounts of materials present in mixture "B"). This difference in carbon pickup is attributed to the difference in characteristics of the types of coke used. From previous experience it has been found that had mixture "A" been melted under identical conditions with 250 lb of by-product coke (instead of beehive), the total carbon in the iron coming from the cupola would have been in the range of 2.75 to 2.85 per cent.

Experience with the use of electrodes (either the graphite or carbon type) indicates that they have practically the same effect on melting temperature and

TABLE 4-COKE TYPE EFFECTS ON CARBON PICKUP

| | Mix | kture "A"* | Mixture "B" | | |
|--------------------------|--------|------------|-------------------|----------|--|
| Materials | Lb | Per Cent | Lb | Per Cent | |
| Silvery pig iron (8% Si) | 100 | 5 | 100 | 5 | |
| Steel Scrap (No. 2) | 1,000 | 50 | 800 | 40 | |
| Automotive Cast Scrap | 900 | 45 | 1,100 | 55 | |
| Totals | 2,000 | 100 | 2,000 | 100 | |
| Carbon in Charge, % | 1. | 72 | 2.01 | | |
| Coke per Charge, lb | 250 (B | eehive) | 250 (By-Product) | | |
| Coke ratio | 8 | :1 | 8:1 | | |
| Cupola size | 72 in | . I. D. | 72 in. I. D. | | |
| Total Carbon** | | | | | |
| Range, % | 3.00 | -3.06 | 3.00-3.08 3.05 | | |
| Average, % | 3. | 04 | | | |

*Mixture "A" contained 4 lb Si in form of 50 per cent lump to bring Si in both charges to same level.

**In each case total carbon determinations were made on 12 consecutive ladles (2 tons each) of the melted iron.

carbon pickup as does pitch coke. Electrodes are mixed with the bed coke in quantities amounting to about 20 per cent by weight and the amount used between charges is from 15 to 20 per cent of the weight of the individual coke charges. As an example of the effect of pitch coke or electrodes on carbon pickup, the comparison shown in Table 5 is given to show the effect of replacing a portion of the beehive coke with these materials. Results of a large number of determinations have shown that when using cupola mixture "C" melted with 300 lb of beehive coke, the total carbon in the melted iron will fall in the range of 3.00 to 3.15 per cent, while the same mixture melted with 250 lb of beehive coke plus 50 lb of pitch coke or electrodes will produce irons having a total carbon in the range of 3.25 to 3.40 per cent.

TABLE 5-EFFECT OF PITCH COKE ON CARBON PICKUP

| | Mix | ture "C" | Mixture"C" | | |
|---|-----------|-------------|------------|-----------|--|
| Materials | Lb | Per Cent | Lb | Per Cent | |
| Malleable Pig Iron | 700 | 35 | 700 | 35 | |
| Steel Scrap (No. 2) | 1,100 | 55 | 1,100 | 55 | |
| Duriron Scrap | 100 | 5 | 100 | 5 | |
| Manganese Steel | 100 | 5 | 100 | 5 | |
| Total | 2,000 | 100 | 2,000 | 100 | |
| Carbon in Charge, % | | 1.66 | | 1.66 | |
| Beehive coke per charge, lb | 300 | | 250 | | |
| Pitch coke per charge, lb | None | | 50 | | |
| Total coke per charge, lb | 300 | | 300 | | |
| Coke Ratio | 6.5 | 7:1 | 6. | 7:1 | |
| Carpola Size | 72 in | I. D. | 72 ir | n. I. D. | |
| Total Carbon, % | 3.00- | -3:15 | 3.25 | -3.40 | |
| Note: Electrodes have pra as pitch coke. | actically | same effect | on carb | on pickup | |

Other mixtures containing 12.5 per cent southern pig (No. 2) and 87.5 per cent cast scrap, when melted with 100 per cent beehive coke in the ratio of 10:1, produce irons having a total carbon in the range of 3.35 to 3.45 per cent, while the same mixtures with the same coke ratio produce irons close to 3.60 per cent total carbon when one-third of the beehive coke is replaced with pitch coke.

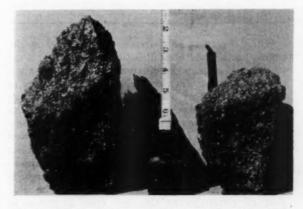


Fig. 9—The two large pieces are carbon electrodes broken, ready for use. Small piece in center is graphite electrode. Note coarse structure of carbon electrode as compared to that of graphite electrode.

Mixtures containing 75 per cent southern pig iron and 25 per cent cast scrap using beehive coke in the ratio of 15:1 yield irons containing slightly above 3.70 per cent total carbon, and these same mixtures when using by-product coke in the same ratio produce irons containing about 3.60 per cent total carbon.

The use of anthracite (coal) as a substitute for a portion of the fuel charge has been successful in a number of instances. Large tonnages of relatively high-carbon iron used for pipe cast centrifugally in water-cooled steel molds have been made from mixtures containing from 15 to 30 per cent pig iron with the balance consisting of cast scrap. For a 3,000-lb metal charge containing 800 lb (26.7 per cent) pig iron, a fuel charge consisting of 250 lb of beehive coke and 50 lb of graphite electrodes has given satisfactory results. On other occasions the fuel charge used consisted of 250 lb of by-product coke and 50 lb of graphite electrodes. With either of these fuel combinations, it has been found possible to replace 50 lb of coke with 50 lb of anthracite and there has been no appreciable change in the carbon content of the iron thus produced. Data in Table No. 6 show a comparison of results obtained both before and after replacing 50 lb of by-product coke with a like quantity of anthracite.

TABLE 6-USE OF ANTHRACITE IN 72-IN. I D CUPOLA

| | Cupola Mixtures | | | | | | |
|----------------------|-----------------|-------|--------|-------|--|--|--|
| Materials | Lb | % | Lb | % | | | |
| Pig Iron | 800 | 26.7 | 800 | 26.7 | | | |
| Cast Scrap | 2,200 | 73.3 | 2,200 | 73.3 | | | |
| Totals | 3,000 | 100.0 | 3,000 | 100.0 | | | |
| Per Charge: | | | | | | | |
| By-Product Coke | 250 lb | | 200 lb | | | | |
| Graphite Electrodes | 50 lb | | 50 lb | | | | |
| Anthracite | None | | 50 lb | | | | |
| Total Fuel | 300 lb | | 300 lb | | | | |
| Fuel Ratio | 10:1 | | 10:1 | | | | |
| Total Carbon, %: | | | | | | | |
| Max | 3.70 | | 3.73 | | | | |
| Min | 3.54 | | 3.58 | | | | |
| Average (10 hr heat) | 3.60 | | 3.64 | | | | |

Note: Total Carbon determinations made every 30 min throughout the heats.

While the effect of electrodes used as fuel in the cupola has already been discussed it is felt that a description of some of the observations made in connection with its use may be of interest. Both types of electrode carbon are considerably stronger and denser than any of the cokes used and it is believed that their behavior in passing through the cupola is in some respects different from that of the cokes. It is well known, of course, that one of the principal functions of the coke in the cupola bed is simply to support mechanically the overlying coke and metal charges at a predetermined height above the tuyeres. The addition of electrodes to the bed coke improves its mechanical strength just as reenforcing bars add mechanical strength to concrete structures. Consequently, a coke bed containing electrodes settles or compresses less under load than one without them. Thus, the

top of the bed remains nearer the predetermined height and consequently remains more open resulting in better blast penetration, somewhat higher melting temperatures, and higher carbon pickup. It is also well known that carbon or graphite electrodes burn when heated to high temperatures in the presence of oxygen. However, it is felt that they burn slower than coke under the conditions existing inside the cupola and at sometime during their descent through the stack are at least partially consumed by being taken into solution in the droplets of molten iron flowing over them. This is borne out by Fig. 10 which shows two pieces of graphite electrodes. The one at the

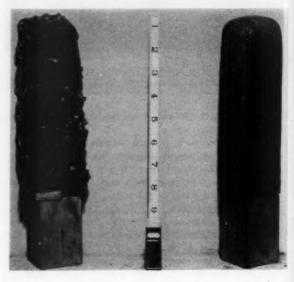


Fig. 10—Graphite electrode. Piece at right is asreceived. Piece at left was taken from cupola drop. Note channels in surface of piece at left resulting from solution in molten iron flowing over it. Light particles adhering to it are droplets of slag.

right is typical of the appearance before placing in the cupola and the one at the left is a piece of the same type of material taken from the "drop" at the end of the heat. The smooth channels in the surface appear to have been formed by erosion as is the case when drops or small streams of water flow over a cake of ice. The sharp edges between the channels are further evidence that this material was consumed by being taken into solution because, had it been consumed by oxidation, it is felt that the sharp edges referred to would have been attacked before portions of the electrode having thicker sections. The lightcolored particles adhering to the electrode are droplets of slag. Examination of a piece of carbon electrode after having passed through the cupola indicates that it behaves in the same manner as the graphite electrode.

While the author's plant has for many years used various types of fuel for the purpose of carbon control, the most logical approach to the carbon control problem is through the use of one kind alone. Inas-

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onrol asmuch as the bulk of the iron for commercial gray iron casting produced in this country is melted with by-product coke, this type has been selected as a basis for the study of carbon control, thus eliminating the variables introduced through the use of combinations of various fuels.

If we are to control the amount of carbon in the iron at the cupola spout we must have an accurate knowledge of the composition of the ingoing charge, including the percentage of carbon in the charge. This is easily determined provided we know the carbon content of the various components. As previously stated we should make determinations for total carbon on each carload of pig iron received with the same care and accuracy used in determining silicon, sulphur, manganese, and phosphorus. Proper handling of home scrap, gates, sprues, and purchased scrap has already been discussed.

In comparing notes on cupola charges, foundrymen and metallurgists frequently refer to a mixture as containing a certain amount of steel, 50 per cent for example. While the percentage of steel is certainly of interest and importance, the writer feels that this figure in itself tells only part of the story because it gives no idea of the amount of carbon in the charge, and it is this figure which (as will be shown later) determines the amount of carbon in the iron at the cupola spout. To illustrate, three cupola mixtures are tabulated:

| Mixture | Charge | Carbon, % |
|---------|---------------------------|------------|
| A | 50% steel containing | 0.30 T. C. |
| | 50% pig iron containing | 4.20 T. C. |
| | Carbon in Charge | 2.25 |
| В | 50% steel containing | 0.30 T. C. |
| | 50% cast scrap containing | 2.90 T. C. |
| | Carbon in Charge | 1.60 |
| C | 25% steel containing | 0.30 T. C. |
| | 75% cast scrap containing | 2.90 T. C. |
| | Carbon in Charge | 2.25 |

From this table it is evident that mixtures A and B will not produce irons of the same carbon content, when melted under like conditions, in spite of the fact that each contains 50 per cent of steel. Likewise, mixtures A and C will produce irons of about the same carbon content even though mixture A contains twice as much steel as mixture C.

As mentioned above, all of the cupolas operated by the author's company are of the front-slagging type which, of course, means that iron flows from the taphole continuously from start to finish of the heat. At no time is there any appreciable accumulation of molten iron in the well of the cupola, but the small amount which does collect remains at a constant level because it flows out as fast as it is melted. Frontslagging cupolas possess many advantages over those tapped intermittently, including uniform carbon pickup, which is one of the essentials of carbon control. Other advantages include hotter iron at the cupola spout, no need for a slag hole with the usual loss of air blast, and better housekeeping because no slag wool is generated. Furthermore, the occasional shutdowns generally necessary for repairing the slag hole can be avoided and the flow of metal from the cupola

may be stopped by simply shutting off the blower, thus eliminating the need for "bots" in the breast hole. This not only makes a safer operation, but also it is practically impossible to run cupola slag into the pouring ladles, as frequently happens with intermittent tapping when the man tapping the cupola leaves the breast hole open a little too long or a bot accidentally comes out.

In cupolas which are tapped intermittently the amount of molten iron in the well may range from several tons (in the larger cupolas) to practically none in the few minutes required to make a tap. This means that the depth of iron within the cupola is constantly changing, and under these conditions the carbon pickup is apt to be less uniform than when the metal is removed as fast as it is melted. It is not inferred that uniform carbon pickup in the cupola cannot be obtained with intermittent tapping, but it requires a little closer supervision. An exact tapping cycle must be established, and the time between taps must then be maintained by means of a stop watch. Frequently, with intermittent tapping, a cupola tapper can see no harm in allowing the time between taps to vary 10, 20 or 30 sec, although actually this may be a high percentage of the established time cycle (between taps), and variations of the magnitude just mentioned may materially effect the percentage of total carbon in the iron tapped, especially in mixtures containing high percentages of steel.

Accurate laboratory records should be kept in which effects of changes in percentage of carbon in the cupola charge, various percentages of silicon and phosphorus in the iron at the cupola spout, and the effects of various types of fuel or combinations thereof are all carefully noted and properly correlated. Based on the data gathered by following this procedure it has been possible to derive an equation and draw a graph, Fig. 11, from which "% Carbon In Iron at Cupola Spout" can be predicted when "% Carbon in Cupola Charge" is known regardless of the relative amounts of steel, cast scrap, and pig iron in the charge. Certain conditions must exist and certain practices must be followed in order that data taken from the

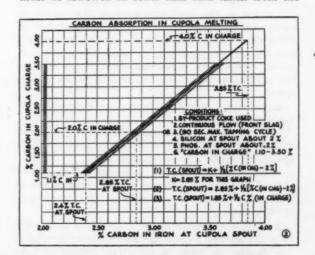


Fig. 11-Carbon absorption in cupola melting.

graph be reliable. They are as follows:

1. Use of by-product coke.

2. Continuous flow of metal from cupola (front

slagging), or

3. Maximum tapping cycle of 90 sec if tapping intermittently. This means 90 sec from the time any one "bot" is removed from the breast until the next one is removed. It does not mean that the cupola is left closed up for 90 sec after the "bot" is put into place. Obviously, this means that study must be given to calibration of the tap hole as iron must be removed from the cupola just about as fast as it is melted.

4. Silicon at cupola spout must be about 2 per cent.

5. Phosphorus in iron at cupola spout must be

about 0.2 per cent.

6. The graph has been used successfully for "calculating" carbon in iron at the cupola spout within the range of 2.40 to 3.60 per cent T. C. This, of course, means than carbon in cupola charge was in the range of 1.10 to 3.50 per cent.

In Fig. 11 the carbon content of the cupola charge is plotted against carbon content of the molten iron at the cupola spout. The equation for this relationship is as follows:

(1) T. C. (spout) = $K + \frac{1}{2}$ (% C in charge = 2.00%), where "T. C. spout" means "per cent carbon in iron at cupola spout." "K" is a constant.

"% C in charge" means "per cent carbon in cupola charge."

For the graph drawn a value of 2.85 per cent has been assigned to K, and this value applies when the six conditions exist in the foregoing. The equation then becomes:

(2) T. C. (spout) = 2.85% + 1/2 (%C in charge — 2.00%), or (3) T. C. (spout) = 1.85% + 1/2 C% (in charge).

The portion of the graph (Fig. 11) representing the range over which it has been used successfully is drawn as a band indicating that the carbon in the iron at the cupola spout can be expected to fall within a range of approximately plus or minus 0.05 per cent.

The graph is used as follows: Suppose the carbon in the charge has been calculated and found to be 2.00 per cent. A horizontal line is drawn intersecting the graph at the level representing "2.00 per cent carbon in cupola charge." From the intersection with the graph a vertical line is drawn so as to intersect the line representing "per cent carbon in iron at cupola spout." This point is found to be 2.85 per cent, or the percentage of carbon in the iron at the cupola spout.

Examination of the graph and/or Equation 2 reveals the following:

1. When the "per cent carbon in the charge" equals 2.00 per cent, the "per cent carbon in iron at cupola spout" will equal 2.85 per cent, or K.

2. Any change made in "per cent carbon in the charge" increases or decreases the "per cent carbon in iron at cupola spout" by an amount equal to one-half of the change.

3. Within the range of the graph, it is noted that there is always an increase in carbon during melting.

4. Beyond the range of the graph (where we have no data at present) it seems probable that when the

"per cent carbon in the charge" is 3.70 the "per cent carbon in iron at cupola spout" will likewise be 3.70, indicating that there is no carbon pickup at this point. When the "per cent carbon in the charge" exceeds 3.70, it seems probable that there will be a reduction or loss in carbon during the melting operation. For example, when the ingoing carbon is 4.00 per cent, it appears that the "carbon in iron at cupola spout" will be 3.85 per cent.

5. The graph indicates clearly that the carbon pickup obtained is greatest for charges containing the least amount of carbon and becomes progressively less as the amount of carbon in the charge increases until finally the carbon in the iron at the cupola spout is actually less than that in the ingoing charge.

CARBON CONTENT OF CUPOLA MELTED IRONS EFFECT OF CHANGING PERCENTAGE OF CARBON IN CHARGE

(3) T. C. (Spout) = $1.85\% + \frac{1}{2}$ C% (In Chg) When Si at Spout is About 2.0% and P About 0.2%

| | Charg | ge No. 1 | Charge No. 2 | | |
|-----------------------|------------|----------|--------------|-------|--|
| | Lbs. | % | Lbs. | % | |
| Malleable Pig | 300 | 15.0 | 300 | 15.0 | |
| Auto | 925 | 46.2 | 1150 | 57.5 | |
| Steel Scrap | 500 | 25.0 | 325 | 16.2 | |
| Silvery Pig | 200 | 10.0 | 150 | 7.5 | |
| Mn—Steel | 75 | 3.8 | 75 | 3.8 | |
| Totals | 2000 | 100.0 | 2000 | 100.0 | |
| Carbon in Charge | 2.4 | 15% | 2.72% | | |
| T. C. Spout-Range | 3.00-3.15% | | 3.15-3.30% | | |
| T. C. Spout-Average | 3.0 | 08% | 3.23% | | |
| (A) Difference Laters | | | 21 0 1 | CIL | |

(A) Difference between carbon in charge No. 2 and Charge No. 1 equals 2.72% minus 2.45% or 0.27%.

(B) Difference between average carbon at spout using Charge No. 2 and Charge No. 1 is 3.23% minus 3.08% or 0.15%. This is approximately 1/2 of the difference in (A) above.

Fig. 12-Effect on carbon pickup of changing percentage of carbon in the charge.

Figure 12 is an example of the effect on carbon pickup of changing the percentage of carbon in the charge when all of the conditions set forth in connection with the graph and Equation 3 of Fig. 11 are satisfied. As indicated in Equation 3 above, increasing carbon in the charge will appear as an increase of onehalf the magnitude in the iron at the cupola spout. Thus as shown in Fig. 12 an increase of carbon in the charge of 0.27 per cent (2.72 to 2.45 per cent) appears as an increase of carbon in the iron at the cupola spout of only 0.15 per cent which obviously is approximately one-half of the change made.

Attention is again called to the practices and conditions under which Equation 3 holds. If there is a change in these conditions a modification is necessary. For example, if silicon in iron at cupola spout is more or less than 2 per cent, calculation for the effect on total carbon is made as follows: Subtract silicon in iron at cupola spout from 2.00 per cent. Multiply this difference by 0.25 and add the produce to the percentage of carbon expected in iron at the cupola spout, as calculated in accordance with Equation 3.

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Example 1—Suppose silicon in iron at cupola spout is 1.60 per cent. Then 2.00 per cent — 1.60 per cent = 0.40 per cent. The product of $0.40 \times 0.25 = 0.10$ per cent, which is the amount to be added to the percentage of carbon expected in iron at the cupola spout.

Example 2—Suppose silicon in iron at cupola spout is 2.40 per cent. Then 2.00 - 2.40 = minus 0.40 per cent. The product of minus $0.40 \times 0.25 = \text{minus } 0.10$ per cent, which is the amount to be added, but adding a minus quantity, of course, means that its numerical value is subtracted. These examples along with the modification which they bring about in Equation 3 are presented in Fig. 13. This relationship has been found to be quite accurate when silicon in iron at cupola spout is within the range of 0.9-2.5 per cent.

CARBON CONTENT OF CUPOLA MELTED IRONS EFFECT OF SILICON IN IRON AT CUPOLA SPOUT

(3) T. C. (Spout) = $1.85\% + \frac{1}{2}\%$ C (In Chg) When Si at Spout is About 2.0% and P About 0.2%

1. Carbon Pickup increases as silicon decreases

2. Carbon Pickup decreases as silicon increases

 Increase or decrease in carbon pick-up equals 1/4 of amount of change in silicon (inversely)

Example No. 1

Suppose silicon is decreased from 2.0% to 1.6% Then: Decrease in silicon = (2.0% - 1.6%) or 0.4% Increase in carbon pickup = $\frac{1}{2}$ \times 0.4% or 0.1%

Example No. 2

Suppose silicon is increased from 2.0% to 2.4% Then: Increase in silicon = (2.4% - 2.0%) or 0.4% Decrease in carbon pickup = $1/4 \times 0.4\%$ or 0.1% Therefore:

(4) T. C. (Spout) = 1.85% + 1/2 C% (In Chg.) + 1/4 (2.0%—Si%) Or:

(5) T. C (Spout)=2.35%+1/2 C% (In Chg.) -1/4 Si% (When Phos at spout is about 0.2%)

Fig. 13—Effect of Equation 5 showing effect on carbon pickup of variations in silicon content of iron at cupola spout.

In view of what has just been said the term " $\frac{1}{4}$ (2.0% — Si %)" has been added to Equation 3 which then becomes,

(4) T. C. (Spout) = 1.85% + ½ C% (In Chg.) + ¼ (2.0%-Si%), where "Si%" means %Silicon in iron at cupola point. (Phosphorus in iron at cupola spout held at about 0.2%)

Adding the term "¼ (2.0%-Si%)" has not destroyed the quality expressed by Equation 3 because one of the conditions imposed was that silicon in the iron at the cupola spout be about 2 per cent. Substituting 2 per cent for the value of "Si%" in the term in question reduces its value to zero and adding zero to either side of an equation does not destroy the equality. Equation 4 can be simplified and rewritten as follows:

(5) T. C. (Spout) =
$$2.35\% + \frac{1}{2}$$
 C% (In Chg.) = $\frac{1}{4}$ Si%.

Figure 14 is taken from records on file in the author's laboratory showing the effect on carbon pick-up of changing the percentage of silicon in the iron at the cupola spout when all conditions except No. 4 as set forth in the graph Fig. 11 are satisfied. As shown, reducing the silicon at the cupola spout from 2 to

CARBON CONTENT OF CUPOLA MELTED IRONS Silicon Effect — Phos at Spout Equals About 0.2% Using — (4) T. C. (Spout) = 1.85% + ½ C% (In Chg.) + ½

(2.0% - Si% = SPT.)Percentages Pounds C Materials Si Mn T. C. Lb Si Mn 0.39 4.26 300 3.51 1.17 12.78 Pig Iron 1.17 2.20 0.60 3.20 800 17.60 4.80 25.60 Automotive Steel Scrap 2.70 0.10 0.40 0.30 900 0.90 3.60 2.00 20.00 4.00 40 0.80 8.00 1.60 Spiegel 2 2.00 Silicon (4 Lb-50% Lump) Totals 2042 24.81 17.57 42.68 % In Charge 1.24 0.88 2.13 0.70 Expected 1.12

* 1.85% + ½ C% (In Chg. = 1.85% + 1.07% = 2.92% * ¼ (2.0% - Si%) = ¼ (2.0% - 1.12%) = 0.22%

* T. C. at Spout (Calculated) 3.14% % S % Si % T. C. % P Analyses % Mn Spout 1.09 0.093 3.15 1.29 0.082 0.73 0.18 3.14 Ladle I Ladle 2 1.27 0.083 0.73 0.19 3.17 0.09 0.20 3.0-3.3 1.0-1.4 0.4 - 1.0Spec. Late Addition

Ladle 1-0.4% Zr + 0.2% GX Ladle 2-0.4% Zr + 0.2% GX

Fig. 14-Effect of carbon pickup of changing percentage of silicon in iron at cupola spout.

1.12 per cent has resulted in a carbon pickup of 0.22 per cent in excess of that which would have been obtained had silicon at the cupola spout not been reduced. This is accounted for by substituting 1.12 per cent for "Si%" in the last term of Equation 4 and solving for the numerical value of this term which is found to be 0.22 per cent.

The effect on carbon pickup of changing the percentage of phosphorus in the iron at the cupola spout has been found to be in the same order of magnitude as changing the percentage of silicon in the iron at the cupola spout. This effect and the development of its influence on Equation 5 are shown in Fig. 15. Based on the above findings, the term "1/4 (0.2%-P%)" has been added to Equation 5 which then becomes,

(6) T. C. (Spout) = $2.35\% + \frac{1}{2}$ C% (In Chg) - $\frac{1}{4}$ Si% + $\frac{1}{4}$ (0.2%-P%),

CARBON CONTENT OF CUPOLA MELTED IRONS

Effect of Phosphorus in Iron at Cupola Spout

(5) T. C. (Spout) = 2.35% + ½ C% (In Chg.) — ¼ Si%

When Phos At Spout Is About 0.2%

Carbon Pickup increases as Phos decreases
 Carbon Pickup decreases as Phos increases

3. Increase or decrease in Carbon Pickup equals 1/4 of amount of change in Phos (inversely)

Example:

Suppose Phos is decreased from 0.60% to 0.20% Decrease in Phos is equal to 0.40% (0.60%-0.20%) Increase in Carbon Pickup will be ½ of 0.40% or 0.10% Increase in Phos will have same numerical effect on Carbon Pickup as a decrease but in opposite direction Then: (6) T. C. (Spout) = 2.35% + ½ C% (In Chg.

Then: (6) T. C. (Spout) = $2.35\% + \frac{1}{2}$ C% (In Chg.) — $\frac{1}{4}$ Si% + $\frac{1}{4}$ (O.2% — P%)
Or: T. C. (Spout) = $2.40\% + \frac{1}{2}$ C% (In Chg.) — $\frac{1}{4}$ Si% — $\frac{1}{4}$ P%

Or: (7) T. C. (Spout) = $2.40\% + \frac{1}{2} C\%$ (In Chg.) — $\frac{1}{4} (Si\% + P\%)$. Spout

Fig. 15—Development of Equation 7 showing effect on carbon pickup of variations in phosphorus in iron at cupola spout.

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where "P%" means percentage of phosphorus in iron at cupola spout. Adding the term "14 (0.2%-P%)" has not destroyed the equality expressed by Equation 5 because one of the conditions imposed was that phosphorus in the iron at the cupola spout be about 0.2 per cent. Substituting 0.2 per cent for the value of "P%" in the term in question reduces its value to zero and, as previously stated, adding zero to either side of an equation does not destroy its equality. Equation 6 has been simplified and rewritten as follows:

(7) T. C. (Spout) = $2.40\% + \frac{1}{2}$ C% (In Chg) — $\frac{1}{4}$ (Si% + P%) spout,

Equation 7 has been found reliable for calculating percentage of carbon in the iron at the cupola spout within the following ranges of composition:

Silicon at cupola spout between 0.9 and 2.5 per cent Phosphorus at cupola spout between 0 and 0.65 per cent

Total Carbon in the charge between 1.1 and 3.9 per cent

Carbon Content of Cupola Melted Irons Using: (7) T. C. (Spout) = $2.4\% + \frac{1}{2}\%$ C (In Chg.) — $\frac{1}{4}$ (Si% + P%) . Spout Phos Effect

| | Mi | xture (| 2 | | M | ixture D | |
|----|------|---------|----------|-----------------|------|----------|---------|
| | % Si | % P | % T. C. | | % Si | % P | % T. C. |
| A. | 1.84 | 0.072 | 3.85 | In Charge | 1.85 | 0.595 | 3.59 |
| B. | 1.66 | 0.097 | | Expected | 1.66 | 0.62 | |
| C. | | | 4.33 2.4 | 1 + 1/2 C In Ch | g. | | 4.19 |
| D. | | | 0.44 | 1/4 (Si + P) | | | 0.57 |
| E. | 4.33 | 0.44 | = 3.89* | Calculated | 4.19 | -0.57 | = 3.62* |
| F. | 1.70 | 0.10 | 3.92 | Analysis | 1.72 | 0.64 | 3.64 |
| | | | Char | rge Materials | | | |
| | 1.72 | 0.013 | 4.07 P | ig Iron-75% | 1.73 | 0.71 | 3.72 |
| | 2.20 | 0.25 | 3.20 A | uto Cast-25% | 2.20 | 0.25 | 3.20 |

Fig. 16-Effect on carbon pickup of changing percentage of phosphorus in iron at cupola spout.

Figure 16 is a comparison of two production runs showing the effect of carbon pickup of changing the percentage of phosphorus in the iron at the cupola spout when all limitations imposed on Equation 7 are satisfied. In Fig. 16 attention is called to the fact that with both mixtures C and D the iron at the cupola spout was excepted to contain 1.6 per cent Si, while with mixture C iron at the cupola spout was

expected to contain 0.097 per cent phosphorus and with mixture D phosphorus was expected to be 0.62 per cent. The influence of phosphorus on carbon pickup appears in the terms "minus $\frac{1}{4}$ (Si% + P%)" and inasmuch as "Si" is the same for both mixtures any differences in the value of this term must be attributed to differences in the value of "P%." For mixture C, " $\frac{1}{4}$ (Si% + P%)" equals minus 0.44 per cent and for mixture D its value is minus 0.57 per cent, the difference obviously being 0.13 per cent. This then is the difference in the amount of carbon picked up by these two mixtures during melting due to the difference in the percentage of phosphorus in the iron at the cupola spout.

Figure 17 shows two examples of how Equation 7 is used in connection with cupola mixtures actually used for production castings, and as can be seen conditions were not limited to irons containing 2 per cent silicon and 0.2 per cet phosphorus at the cupola spout as was the case when calculating percentage of total carbon in the iron at the cupola spout in accordance with Equation 3. For all practical purposes, mixtures A and B will produce irons containing the same percentage of phosphorus. Likewise, the percentage of "carbon in the charge" is nearly the same in both cases, and for this reason Fig. 17 may be considered an example of the effect of silicon on carbon pickup. This can be seen by inspection of the term "1/4 (Si + P)" which for mixture A has a numerical value of 0.26 per cent and for mixture B a value of 0.47 per cent, or a difference of 0.21 per cent. This is largely due to the difference (0.8 per cent) in percentage of silicon expected in the irons produced by the mixtures in question.

For the sake of convenience, Equation 7 has been put into the form of an alignment chart as shown in Fig. 18. Equation 17 can be looked upon as containing three unknown quantities, namely "(Si + P)," "C," and "T. C." and these terms appear as headings over the vertical scales in the alignment chart. The chart is used by assigning values to two of the unknowns and solving for the third. It contains all solutions for Equation 7 and any one of the three unknowns can be determined after assigning values to the other two. This is done by placing the chart on a flat surface and drawing a line (or laying a straight-

| | CARBON CONTENT OF CUPOLA MELTED IRONS | |
|--------|--|--|
| Using: | (7) T. C. (Spout) = $2.4\% + \frac{1}{2}$ C% (In Chg.) $-\frac{1}{4}$ (Si% + P%) . Spout | |
| | SILICON EFFECT | |

| | | Mixture | 1 | | | | | Mixture | В | |
|----|-----------|--------------|------------|---------|---------------|---|---------------------|--------------|--------|--------|
| | %Si | %Mn | %P | %T. C. | | * | %Si | %Mn | %P | %T. C. |
| A. | 1.03 | 0.86 | 0.084 | 1.69 | In Charge | | 1.92 | 1.15 | 0.105 | 1.63 |
| B. | 0.93 | 0.69 | 0.109 | | Expected | | 1.73 | 0.92 | 0.13 | |
| C. | 2.4 + 1 | 2 C In Chg. | | 3.25 | | | $2.4 + \frac{1}{2}$ | C In Chg. | = | 3.22 |
| D. | 1/4 (Si . | + P) . Spout | = | 0.26 | | | 1/4 (Si + | P) . Spout | = | 0.47 |
| E. | 3.25 Les | s 0.26 | = | 2.99* | Calculated | | 3.22 Less | 0.47 | === | 2.75* |
| F. | 1.13 | 0.71 | .11 | 2.95 | Analysis | | 2.02 | 0.97 | 0.11 | 2.78 |
| G. | (0.14) (0 | 0.2% Zr + 0. | 1% Fe . Si | | Late Addition | | (0.22) | (0.9% Me | o-Si) | |
| H. | 0.8/1.2 | 0.4/1.0 | .14/.18 | 2.8/3.0 | Spec. | | 50,000 psi | i Mn. — 2-in | . Bars | |

Fig. 17—Examples of method of calculating percentage of carbon in iron at cupola spout in accordance with Equation 7.

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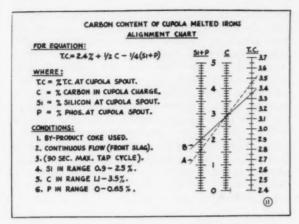


Fig. 18—Carbon content of cupola melted irons; alignment chart.

edge) between the two points representing known values on any two of the three vertical scales. The figure sought will be found along the straight line where it intersects the third vertical scale. Suppose, for example, that it is required to produce an iron with 1.8 per cent silicon, 0.2 per cent phosphorus, and 3.35 per cent total carbon at the cupola spout. Having this information it is necessary to determine "C," or the percentage of carbon in the cupola charge. The value of "(Si + P)" is 2.0 and the value of "T. C." is 3.35. The line, labelled "B" in the chart, drawn between these two points intersects the scale headed "C" at the 2.9 level. From this it is known that there must be a 2.9 per cent carbon in the charge in order to produce iron of the required composition at the cupola spout.

Alignment Chart

A study of Equation 7 and the alignment chart indicates that the cupola melting process is partially selfcompensating from the standpoint of carbon equivalent of the iron at the spout. This is a fortunate situation for the cupola operator. As has already been shown, any change made in the percentage of carbon in the charge increases or decreases the percentage of carbon in the iron at the cupola spout by an amount equal to only one-half of the change. Thus any errors effecting the percentage of carbon in the charge would be reduced by 50 per cent by the time the iron is tapped from the cupola. Suppose in a 1-ton cupola charge 100 lb of steel containing 0.3 per cent T. C. was through error substituted for 100 lb of pig iron containing 4.0 per cent T. C. Such an error, all other things being equal, would reduce the carbon in the iron at the cupola spout by an amount slightly less than 0.10 per cent. An error of the same magnitude substituting (100 lb) cast scrap containing 3.0 per cent carbon for pig iron containing 4.0 per cent carbon would reduce the carbon in the iron at the cupola spout by only 0.025 per cent which is, of course, a negligible amount. Assuming that an operation where the total carbon varies within a range of plus or minus 0.10 per cent is considered satisfactory, it can be seen

that some minor variations or errors in weighing can be tolerated, although they are by no means recommended. Occasionally it will be reported that the carbon dropped 0.20 or 0.30 per cent during the course of a heat. If this drop were due entirely to poor weighing practice, it might well represent a substitution of 200 or 300 lb of steel for a corresponding amount of pig iron. If the drop was due to substituting all cast scrap for pig iron, the error in weighing would be correspondingly greater. It is, of course, realized that there could be a combination of errors in weighing, but in any event errors of the magnitude in question are inexcusable. Probably variations of 0.20 to 0.30 per cent of carbon could be attributed not only to poor weighing, but also to a bad condition inside the cupola along with a complete lack of knowledge of the carbon content of the charge.

Possible Errors in Operation

In case errors creep into the operation which bring about an increase in the amount of silicon and/or phosphorus in the iron, an amount of carbon (over and above that determined by calculation) equal to one-fourth of the increase will be excluded from the iron at the cupola spout. Conversely a decrease in the amount of silicon and/or phosphorus will allow the charge to absorb an additional amount of carbon equal to one-fourth of the decrease. These more or less automatic adjustments are definitely favorable to the operator inasmuch as they very nearly satisfy generally accepted expressions for carbon equivalent.

Lines labeled "A" and "B" respectively in alignment chart show graphically the effect of an increase or decrease amounting to 0.40 per cent in silicon plus phosphorus in the iron at the spout. Reducing the silicon plus phosphorus from 2.0 per cent (line "B") to 1.6 per cent (line "A") and maintaining 2.9 per cent carbon in the charge in both cases (see vertical scale headed "C") has increased the total carbon from 3.35 to 3.45 per cent or a gain of 0.10 per cent (see vertical scale headed "T. C."). The carbon equivalents of the irons represented by lines "A" and "B" are 3.98 and 4.02 per cent respectively, and for practical purposes these would be considered the same. However, this difference in carbon equivalent could be compensated for by a late addition of 0.12 per cent silicon to iron "A." Thus a decrease of 0.40 per cent silicon in the iron at the cupola spout has resulted in this case in a net saving of 0.28 per cent silicon. Based on 75 per cent grade ferrosilicon at a cost of \$0.11 per lb this is equal to a saving slightly in excess of \$1.00 per ton of iron.

The discussion of carbon control has been based on the use of by-product coke for melting in front-slagging cupolas. As previously stated, it is not implied that good results cannot be obtained with intermittent tapping and some authorities state that comparable results will be obtained if a 90-sec maximum tapping cycle is maintained. In case a longer tapping cycle is found to be convenient there will be no harmful effects, but it will be found that these cupolas (tapped intermittently) are less sensitive to changes in percent of carbon in the charge or changes in percentages of silicon and phosphorus at the spout than those

provided with front-slagging spouts, and in some operations this may be looked upon as an advantage. The disadvantage of a long tapping cycle lies in the fact that it is quite difficult to produce low total carbon irons when required.

Coke Ratios

Reference to coke ratios has been deliberately avoided as this is a matter which must be handled by each individual cupola operator. However, let us assume that a cupola is melting good, hot iron and at the same time the carbon in the charge is 3.00 per cent. Let us assume further that the operator is later called upon to produce a grade of iron calling for a charge containing only 2.00 per cent carbon and that the weight of the charge is 4,000 lb in each case. The carbon picked up by the first charge mentioned will be 0.35 per cent, while that picked up by the other will be 0.85 per cent, or a difference of 0.50 per cent. This means that a 4,000-lb charge with ingoing carbon of 2.00 per cent will pick up 20 lb more of carbon than the same size charge with ingoing carbon of 3.00 per cent. Twenty pounds of carbon is equivalent to about 22 lb of coke, so it would be wise to increase the coke charge by this amount.

In other words, when using a charge of such a nature that a considerable amount of carbon is absorbed during the melting operation, care must be taken to supply sufficient coke to properly melt and superheat the iron, after having made due allowance for the equivalent amount absorbed by the charge.

Under certain conditions the cupola practice may be deliberately manipulated to bring about some desired result. The use of beehive coke alone or in combination with pitch coke is a deliberate manipulation intended to produce irons higher in total carbon content than could be obtained when melting the same charge with by-product coke alone. In other instances, a combination of by-product and pitch coke is used to melt charges having a lower percentage of carbon in the charge than would normally be used when melting with by-product coke alone.

It may be of interest to note that in the author's experience, when using "good" coke, any manipulations of the sort referred to have always resulted in an increase in the value of K in Equation 1 of Fig. 11 or of the constant 2.4 per cent in Equation 7. On the occasions when there has been a decrease in the value of K, it has almost always been traceable to the use of coke not intended primarily for cupola use. This we have referred to as "bad" coke. In this connection, the author wishes to point out that Equation 7 is a good yardstick for the measurement of coke quality, although a discussion of this subject is beyond the scope of this paper.

Furthermore, it would probably be well to introduce a "coke" factor f and assign to f a value of one for what we consider "good" coke (or a combination of cokes), a value greater than one for "extra good" coke, and a value less than one for "bad" coke.

Equation 7 would then be written:

(8) T. C. (spout) = 2.4% . (f) + $\frac{1}{2}$ C - $\frac{1}{4}$ (Si + P)

In the above discussion of carbon control, no men-

tion has been made of the possible influence of sulphur on carbon pickup, and generally speaking sulphur present in "normal" amounts has no appreciable effect. However, it has been found in cases where scrap iron or other materials with excessively high sulphur content find their way into the cupola that the carbon pickup will be drastically reduced. On one occasion a 72-in. cupola was being used to melt iron for use in pipe cast centrifugally in water cooled steel molds. This cupola is operated in connection with a 10-ton T-pot ladle where some of the sulphur is removed by means of fused soda ash. A log of the changes in composition which were recorded in connection with a particular heat is shown in Table 7, which follows:

TABLE 7-EFFECT OF SULPHUR ON CARBON PICKUP

| | % Sul _I | ohur In | |
|---------------|--------------------------|------------------------|------------------------|
| Time A. M. | Iron at Cupola Spout | Iron at T-Pot Spout | % T. C. T-Pot Spout |
| 7:30 | 0.120 | 0.059 | 3.64 |
| 8:00 | connect | 0.159 | 3.41 |
| 8:30 | _ | - | 3.32 |
| 9:00 | 0.280 | 0.166 | 3.25 |
| (Note: | Iron at cupola spout con | tained about 1.7% | Si) |

Investigation revealed that a quantity of carbon bisulphide retort scrap containing over 20 per cent sulphur had been charged into the cupola. In view of this and other observations it was felt that the high percentage of sulphur in the iron was not due to faulty operation of the type frequently accompanied by "foaming" of the slag, a condition which generally produces irons abnormally high in sulphur and low in total carbon. It is believed that the drop in carbon of practically 0.40 per cent in the space of 90 min was due to the unusually high sulphur content of the charge. This has greatly reduced the amount of carbon pickup during the melting operation.

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Summary

1. The cupola furnace is a high speed, continuous melting unit capable of producing a variety of high-quality engineering materials at relatively low melting cost. It is unique in that melting stock and fuel are in intimate contact during the melting operation resulting in constantly changing physical and chemical properties.

2. The cupola is used either directly or indirectly in the production of: (a) "Plain" cast irons ranging from about 25,000 to 40,000 psi tensile strength; (b) Alloyed irons ranging from about 40,000 to 70,000 psi; (c) Spheroidal graphite irons with tensile strengths in excess of 100,000 psi with measurable ductility; (d) Malleable iron castings; (e) Duriron; (f) Ni-Resist; (g) Ni-Hard; (h) Manganese steel; (i) Bessemer steel; (j) Open Hearth steel; (k) Copper; (l) Bronze; (m) Mineral wool for insulating purposes.

3. Cupola coke "bed" practice is of utmost importance. All of the coke including that between the sand bottom and the tuyeres should be thoroughly ignited before charging starts.

4. For uniformity, a definite sequence for placing components of the mixture into the cupola or charg-

ing buckets should be followed. For maximum carbon pickup, components containing the lowest percentage of carbon should be next to the fuel and those containing the highest percentage of carbon furthest from the fuel charge.

5. The operator must have an exact knowledge of the composition of all metallic components of the charge including total carbon. The carbon content of

the ingoing charge must be calculated.

6. Carbon pickup during melting is influenced by the amount and type of fuel used in the coke bed and between charges. Combinations of two or more types of fuel are useful in helping to control the carbon content of the final product.

7. The importance of accurately weighing all raw materials cannot be over-emphasized. Air is a raw material used in large quantities and should be accu-

rately metered or weighed.

8. Dry blast and hot blast equipment have a marked influence on cupola operations. All other things being equal, they result in higher melting temperatures, higher total carbons and lower losses of silicon and manganese. Good mechanical charging and controllable settings of tuyeres help maintain uniform conditions inside the cupola.

9. Front-slagging, continuous-flow cupolas possess many advantages including higher metal temperature at the spout, uniform carbon pickup, lower sulphur pickup, better plant housekeeping, better safety, a more positive separation of cupola slag from the molten iron, and the ability to produce low-carbon

irons when necessary.

10. Forehearth ladles having a capacity equivalent to several cupola charges help maintain a high order of uniformity of metal composition and afford an op-

portunity for sulphur reduction and structure control through inoculation while the iron is being transferred to the pouring ladles.

11. By imposing certain conditions on the cupola operation it has been possible to derive an equation for calculating the percentage of carbon in the iron at the cupola spout when percentage of carbon in the charge and percentage of silicon and phosphorus at the cupola spout are known, regardless of the relative amounts of steel, cast scrap, and pig iron in the charge. The equation is written:

T. C. (Spout) =
$$2.4\%$$
. (f) + $\frac{1}{2}$ C - $\frac{1}{4}$ (Si +P)

12. "Abnormally" high sulphur content of the charge drastically reduces carbon pickup during melting in acid-lined cupolas.

Acknowledgments

The author wishes to thank S. C. Massari, Technical Director of A.F.S., for much valuable data regarding materials other than cast iron melted in the cupola as well as for suggestions regarding the arrangement of material in the manuscript; D. J. Reese of International Nickel Company for many suggestions regarding cupola operation given the writer during the past fifteen years; D. E. Krause, Executive Director of Gray Iron Research Institute, for permission to present data regarding the effect of moisture in the cupola air blast; Gosta Vennerholm of Ford Motor Company, Max Kuniansky and R. M. Rubush of Lynchburg Foundry Company for helpful suggestions regarding arrangement of material in the manuscript; and T. W. Curry, H. Henderson, and H. K. McGavock of Lynchburg Foundry Company for help in obtaining data regarding front slagging cupolas.

ALUMINUM ALLOY CASTINGS A REVIEW OF BRITISH ACHIEVEMENT

By

Frank Hudson, F.I.M.*

Introduction

ON LOOKING BACK over the past 50 years, the British founder of aluminum alloy castings can well be proud of his achievements. Although the "youngest" member of the foundry industry, he has outstripped all other sections in regard to progressive outlook on development and production of castings

of approved quality.

Production of aluminum alloy castings was first established in Great Britain with the introduction of the automobile, at the turn of the century, and since then growth has been rapid, to meet the needs of the aircraft industry. In 1937 the output of castings was around 12,000 tons per year, and during the second World War a peak output of approximately 60,000 tons was reached, in 1943-44 almost solely for aircraft purposes. Today production stands in the neighborhood of 45,000 tons per year, of which 51 per cent is composed of gravity die-castings (permanent-mold castings), 35 per cent sand castings and 14 per cent pressure die-castings. Statistics supplied by The Aluminum Development Association show that the present application of castings in various industrial fields is as follows:-

Automobiles, Road and Rail Transport, etc.

42 to 45 per cent

Building (including interior fittings)

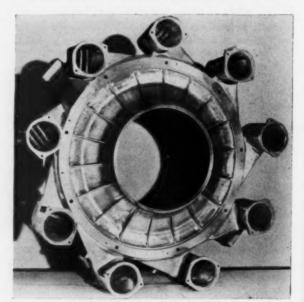
12 to 15 per cent

Domestic Appliances (cookers, refrigerators,

| Domestic Appliances | (COOKEIS, TEITIGETATOIS, | | |
|----------------------|--------------------------|-----|------|
| pots, pans, etc.) | | | cent |
| Electrical Equipment | 8 to 9 | per | cent |
| Aircraft | 6 | per | cent |
| Machine Tools | 4 | per | cent |
| Textile Machinery | 3 | per | cent |
| Shipbuilding. | + 1 | per | cent |
| Mining Equipment | 1 | per | cent |
| Miscellaneous | 7 to 10 | per | cent |

Figures 1 to 12 illustrate a few of the many modern engineering and industrial applications of aluminum alloy castings which are of interest to the foundryman.

The early castings, mainly produced in alloys of aluminum with zinc and copper, had relatively low mechanical properties, and their use in engineering was largely confined to such items as handwheels, crankcases, covers and other lightly stressed parts. The developments in aero-engine construction which took place in 1914-1918, during the first World War, gave rise to an insistent demand for a better aluminum alloy than was then avaliable, having greater strength, particularly at elevated temperatures. In an endeavor to meet this demand, a research on the subject was undertaken by The National Physical Laboratory. The outcome of this work, published in 1921, was the discovery of the excellent properties exhibited by



(Courtesy Rolls-Royce Ltd.)

Fig. 1—Darwent 5 gas turbine compressor casting sand cast in R.R.50 alloy. The guide vanes are made from extruded bar and "cast-in."

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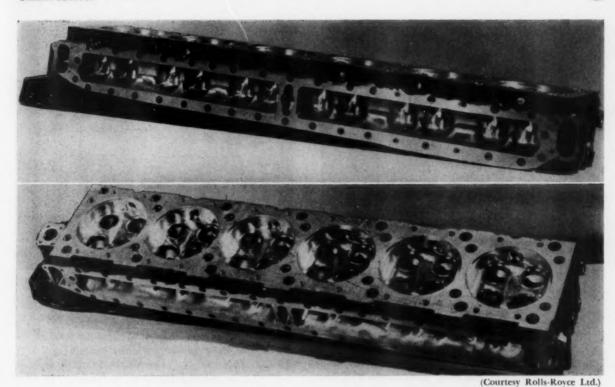
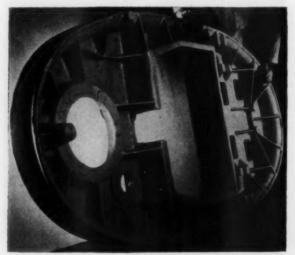


Fig. 2-Merlin aero-engine cylinder head sand cast in R.R.50 alloy.



(Courtesy High Duty Alloys Ltd.)

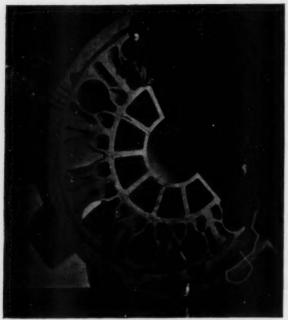
Fig. 3—Front casing for DeHavilland Goblin gas turbine sand cast in Hiduminium R.R. 50. Diameter at intake face 37½ in. Center bore 3½ diam with four strengthening ribs ½ in. thick. Depth from intake face to outlet face 17 in. Elongated outlets 17 in. x 9 in. Entire weight 138 lb.



(Courtesy High Duty Alloys Ltd.)

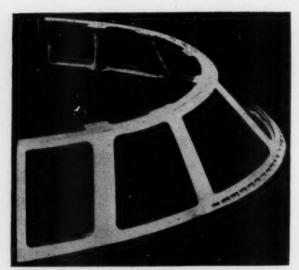
Fig. 4—Chassis for Dodgem car sand cast in Hiduminium R.R. 50. Length 5 ft, 11 in., width 29½ in. Outside rim 5% in. with an average wall thickness of ½6 in.

complex alloys of aluminum containing copper, nickel and magnesium. The alloy finally selected as giving the best all-around properties was one having the nominal composition—copper 4 per cent, nickel 2 per cent, magnesium 1.5 per cent, remainder aluminum.



(Courtesy High Duty Alloys Ltd.)

Fig. 5-Armstrong Siddeley jet air intake throat sand cast in Hiduminium R.R. 50. Overall diam 52 in. Wall thickness 0.175 in. Weight 160 lb.



(Courtesy J. Stone & Co. Ltd.)

Fig. 6-Tudor aircraft windscreen frame sand cast in Ceralumin A.S.M. To avoid distortion this casting is heat treated on formers.

For convenience of nomenclature this alloy was named Y-alloy; it is used in both the as-cast and heat-treated conditions.

The rapid development of high-strength aluminum alloys in Great Britain dates from the advent of Yalloy, and the value of this material for internalcombustion-engine pistons and other engineering components operating at elevated temperatures has re-



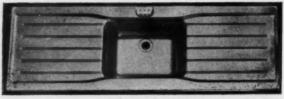
(Courtesy William Mills Ltd.)

Fig. 7—Air delivery casing for gas turbine sand cast in B.S. 1490-LM-7-P aluminum alloy containing 2.5 Si, 2.0 Cu, 1.0 Ni, 1.0 Fe. Dimensions, 28 in. diam by 20 in. high. Fettled weight 121 lb.



(Courtesy Rolls-Royce Ltd.)

Fig. 8-Merlin supercharger drive rear-half assembly casing gravity die-cast in R.R. 50.



(Courtesy Northern Aluminum Co. Ltd.)

Fig. 9—Double-draining board type of sink unit. Gravity die-cast in B.S. 1490-LM-6-M aluminum alloy containing 10-13 Si. Dimensions, 63 in. x 21 in. x 3/16 in. thick. Bowl 19 in. x 15 in. x 8 in. deep. This is probably the largest gravity die-casting, in terms of surface area, so far produced in Great Britain.

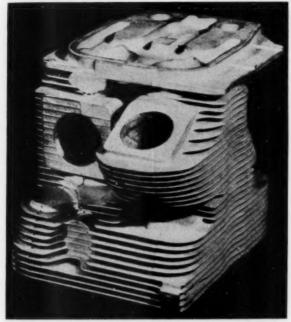
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Fig

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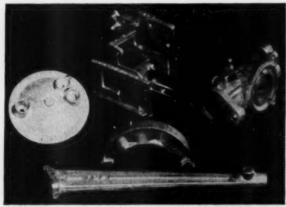
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Fig.



(Courtesy Northern Aluminum Co. Ltd.)

Fig. 10-Air-cooled aircraft-engine cylinder head cast in Y alloy by the evacuated-die process.

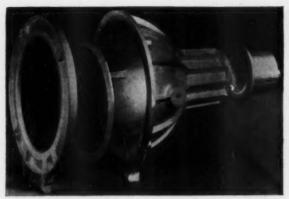


(Courtesy Metal Castings Ltd.)

Fig. 11—Miscellaneous pressure die-castings in aluminum alloy B.S. STA.7/AC. 2 containing 9.0 to 11.5 Si and 1.5 Ni.

Casting Alloys in Use Today

Although Y-alloy was developed over 30 years ago, it still remains one of the best alloys for special-purpose applications and is extensively employed, in the cast form, for all types of automobile, motor-cycle and diesel engine pistons. (In the case of aero-engines, modern practice favors the use of forged pistons rather than castings.) After heat-treatment the alloy has a high elastic limit, and good resistance to corrosion, and its mechanical properties are well maintained at elevated temperatures, as will be observed from Fig. 13. Caven and Keeble,² in a recent paper, pro-



(Courtesv Non-Ferrous Die Casting Co. Ltd.)

Fig. 12—Searchlight components die-cast in B.S. 1490-LM-6-M aluminum-silicon alloy by the low pressure process. Body 14 in. diam x 11 in. long. Cover 15 in. diam. Bezel 12 in. diam. Cap 6 in. diam x 2 in. long.

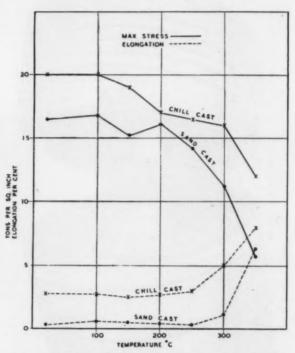


Fig. 13—Mechanical properties of Y alloy at elevated temperatures. 1-in. diam cast bars. Solution treated 520 C (968 F), 6 hr and quenched in boiling water.

Aged 5 days at room temperature.

vide an interesting example of the reliability of Y-alloy under severe operating conditions.

Figures 14 and 15, taken from this paper, show a large composite diesel engine piston made up of a piston-crown and skirt, having a total weight of approximately 450 lb. These castings are fastened together by eight 2-in. diam nuts and bolts. On one occasion a 2-in. nut came adrift and hammered up and down between the crown and skirt, with each revolution of the engine, producing a stress in the

nature of a repeated impact test. After about 385 hr (some millions of reversals) the nut punctured the piston crown, as illustrated in Fig. 15, causing only loss of compression and blowing out of the crankcase inspection doors. This incident adequately demonstrates the strength and toughness of Y-alloy piston castings under service conditions.

Y-alloy is not an easy alloy to handle in the foundry, being susceptible to oxide formation and intercrystal-line shrinkage porosity. It was a natural consequence, therefore, that the founder should endeavor to improve its casting properties. With this in mind, some years later, the R.R. group of aluminum alloys was developed and patented by Rolls Royce Ltd. and manufacture was taken over by High Duty Alloys, Ltd. Casting alloys such as Hiduminium, R.R.50, R.R.53, and R.R.53B are in common use today for a wide variety of applications. The original R.R.50 alloy,



Fig. 14-Large Y alloy diesel engine composite piston weighing approx 450 lb, which was damaged by loose 2-in. nut.



Fig. 15—Crown of Y alloy composite piston showing puncture caused by loose 2-in. nut.

developed for sand and die castings for general purposes, is characterized by excellent founding properties. Unlike most high-strength light alloys, R.R.50 requires no solution treatment, and only a low-temperature precipitation treatment is needed to develop optimum properties. This feature is particularly valuable in the production of large castings of varying section, since internal stresses, which frequently follow severe quenching, are avoided.

Castings in this alloy are not subject to age-hardening in service and are regularly employed for water cooled cylinder blocks and heads, crankcases and other parts in automobile and aircraft manufacture which demand a high standard of performance. This alloy is also used today for air intakes, compressor stator casings and bearing housings on gas turbines. The other two alloys, R.R.53 and 53B, on account of the high degree to which they retain their strength and hardness at elevated temperatures, have been developed principally for such castings as automobile and motorcycle pistons, and for parts other than pistons, such as air-cooled cylinder heads, operating at high temperatures in aircraft engines.

During recent years a range of rather similar alloys has been developed by J. Stone and Co., Ltd., under the name of Ceralumin⁴ B, C, D, and A.S.M. In these alloys grain refinement is effected by means of a small addition of niobium instead of titanium. Ceralumin B and A.S.M. are probably the most widely used alloys in this group. Like R.R.50 alloy, the outstanding features of Ceralumin B are its excellent castability and the useful mechanical properties obtained by a simple low-temperature heat-treatment operation. Ceralumin A.S.M. has high strength, reasonable duc-

Table 1-Composition of Aluminum-Alloy Castings for General Engineering Purposes (B.S. 1490 : 1949)

| | | IV | Co | 3 | Ma | Eo | Mn | N: | Zn | ph | Sn | Other Elements |
|--|--|-----|---------|---------|---------|------|---------|------|----------|-------|---------|-------------------------------|
| The second secon | and the sale of th | TW | Cu | 10 | MR | re | IIIIAT | 181 | TO THE | 0.7 | - | |
| Al-Cu-Si-Zn | BS1490-LM-1-M | Bal | 6.0-8.0 | 2.0-4.0 | 0.15 | 1.0 | 9.0 | 0.5 | 2.0-4.0 | 0.3 | 0.2 | Total Mn, Ni, Sn and Ph |
| | | | | | max | max | max | max | | max | max | not to exceed 1.0 per cent |
| Al-Zn-Cu | BS1490-LM-3-M | Bal | 2.5-4.5 | 1.3 | 0.10 | 1.0 | 0.5 | 0.5 | 9.0-13.0 | 0.3 | 0.2 | |
| | | | | max | max | max | max | max | | max | max | |
| Al-Si-Cu | BS1490-LM-4-M | Bal | 2.0-4.0 | 4.0- | 0.15 | 8.0 | 0.3-0.7 | 0.35 | 0.5 | 0.1 | 0.05 | Ti 0.2 max. Total Mn and |
| | | | | 0.9 | max | max | | max | тах | max | max | Fe not to exceed 1.3 per cent |
| Al-Mg | BS1490-LM-5-M | Bal | 0.1 | 0.3 | 3.0-6.0 | 9.0 | 0.3-0.7 | 0.1 | 0.1 | 0.05 | 0.05 | |
| , | | | max | max | | max | | max | max | max | max | |
| Al-Si (Alpax) | BS1490-LM-6-M | Bal | 0.1 | 10.0- | 0.10 | 9.0 | 0.5 | 0.1 | 0.1 | 0.1 | 0.05 | , |
| | | | max | 13.0 | max | max | max | max | max | max | max | |
| R.R.50 | BS1490-LM-7-M | Bal | 1.0-2.5 | 1.5 | 0.05 | 0.3 | 0.1 | 0.5- | 0.1 | 0.1 | 0.05 | Ti or Nb 0.05-0.30 singly |
| Ceralumin B | BS1490-LM-7-P | | | 3.5 | 0.20 | 1.4 | max | 1.7 | max | max | max | or in total |
| Al-Si-Mg | BS1490-LM-8-W | Bal | 0.1 | 4.5 | 0.2- | 9.0 | 0.3 | 0.1 | 0.1 | 0.1 | 0.05 | |
| | BS1490-LM-8-WP | | max | 0.9 | 9.0 | max | 0.7 | max | max | max | max | |
| Al-Si (Alpax Beta) | - | Bal | 0.1 | 10.0- | 0.2 | 9.0 | 0.3 | 0.1 | 0.1 | 0.1 | 0.05 | |
| | BS1490-LM-9-WP | | max | 13.0 | -9.0 | max | 0.7 | max | max | max | max | |
| Al-Mg | BS1490-LM-10-W | Bal | 0.1 | 0.35 | 9.5- | 0.35 | I | 1 | 1 | 1 | - | Ti 0.2 max |
| | | | max | max | 0.11 | max | | | | | | |
| Al-Cu | BS1490-LM-11-W | Bal | 4.0- | 0.25 | 1 | 0.25 | 1 | 1 | 1 | Ī | 1 | Ti or Nb 0.05-0.30 singly |
| | BS1490-LM-11-WP | | 5.0 | max | | max | | | | | | or in total |
| Al-Cu | BS1490-LM-12-WP | Bal | -0.6 | 2.0 | 0.15 | 0.5- | 9.0 | 0.5 | 0.1 | 0.1 | 0.1 | Total Mn and Fe not to |
| | | | 10.5 | max | 0.35 | 1.5 | max | max | max | max | max | exceed 1.5 per cent |
| Lo-Ex | BS1490-LM-13-WP | | 0.5- | 11.0- | -8.0 | 8.0 | 0.5 | 2.0- | 0.1 | 0.1 | 0.1 | |
| | BS1490-LM-13-WP | Bal | 6.1 | 13.0 | 1.5 | max | max | 3.0 | тах | max | max | |
| | (special) | | | | | | - | 3 | | 3 | 40.0 | 000 |
| Y Alloy | BS1490-LM-14-M | | 20.00 | 9.0 | 1.2- | 9.0 | 9.0 | ap o | 0.1 | 0.05 | 0.05 | 11 0.2 max |
| | BS1490-LM-14-WP RS1490-LM-14-WP | Bal | 4.5 | max | 1.7 | max | max | 2.3 | max | шах | Max | |
| | (Special) | | | | | | | | | | | |
| R.R.53B | BS1490-LM-15-WP | Bal | 1.3 | -9.0 | 0.5- | -8.0 | 0.1 | 0.5- | 0.1 | 0.05 | 0.02 | Ti or Nb 0.05-0.30 singly |
| Ceralumin C | | | 3.0 | 2.0 | 1.7 | 1.4 | max | 2.0 | max | тах | max | or in total |
| Al-Si-Cu | BS1490-LM-16-W | Bal | 1.0- | 4.5- | -4-0 | 9.0 | 0.5 | 0.25 | 0.1 | 0.05 | 0.05 | |
| | BS1490-LM-16-WP | | 1.5 | 5.5 | 9.0 | max | max | max | max | max | max | |
| Birmasil Special | BS1490-LM-17-M | Bal | 0.1 | 10.0- | 0.1 | 9.0 | 0.5 | 19.5 | 0.1 | 0.1 | 0.05 | |
| | | | max | 13.0 | max | max | max | 3.5 | max | max | max | |
| Al-Si | BS1490-LM-18-M | Bal | 0.1 | 4.5 | 0.1 | 9.0 | 0.5 | 0.1 | 0.1 | 0.1 | 0.02 | |
| | | | max | 0.9 | max | max | max | max | max | max | max | |
| Aeral A | BS1490-LM-19-W | Bal | 2.0- | 0.7 | 0.5- | 8.0 | -1.0 | 1 | 0.5 | 0.05 | 0.05 | Cd 0.5-2.5 |
| | | | 4.5 | max | 9 | max | 0.0 | 10 | 0.9 | 0.1 | 0.05 | ** COO C. ** |
| | ֡ | | | | | | | | | 11.11 | 17.77.5 | |

tility and good resistance to shock, which makes it particularly attractive for a wide range of applications.

Latest developments indicate that for certain specialized service conditions the R.R. and Ceralumin alloys may be superseded by the aluminum-magnesium alloys, in view of the much greater ductility of the latter. It is worth noting, however, that these latter alloys are difficult to handle in the foundry and require specialized technique for successful production. Furthermore, they are not suitable for service at elevated temperatures, in which field Y, R.R. and the Ceralumin alloys remain supreme.

The light weight of aluminum alloys, combined with their high thermal conductivity, gives them definite advantages over cast iron for automobile and other pistons. Since, however, most aluminum alloys have a coefficient of linear expansion of the order of 23 millionths per °C, as compared with 12 millionths per °C for cast iron, attempts have been made to overcome the differential expansion between the aluminum alloy piston and the cast iron cylinder or cylinder liner. In the aluminum alloy known as Lo-Ex, advantage is taken of the fact that silicon in sufficient quantity (14 per cent) considerably reduces expansion while the addition of 2 per cent nickel increases hardness and improves the properties at elevated temperatures.

The plain high-silicon type of aluminum alloy possesses excellent castability, coupled with good resistance to corrosion, but gives rather a low value for proof stress. Taking this material as a basis, The Birmingham Aluminum Casting (1903) Co. Ltd., have developed an alloy known as Birmasil Special.⁵ By adding nickel, up to $3\frac{1}{2}$ per cent, they have produced an alloy having high tensile strength in the as-cast condition, coupled with good mechanical properties. This firm also developed the P.2 aluminum alloy containing 4.0 to 5.0 per cent silicon, 1.75 to 2.5 per cent nickel, a material which is especially suitable for the production of pressure die-castings to close dimensional tolerances.

In addition to the special alloys already mentioned, there are many other compositions of aluminum-zinccopper, aluminum-copper, aluminum-silicon-copper, etc., in use for less important applications.

During recent years a multiplicity of aluminumbase alloys has come into being, many of which are redundant and not justified by any special properties. There is real need for standardization in this direction. The introduction of British Standard 1490:1949 last year, on "Aluminum and Aluminum Alloy Ingots and Castings for General Engineering Purposes," by the British Standards Institution, limiting the number of alloys to twenty, is a step in the right direction. Tables 1 and 2 give representative compositions and properties of some aluminum alloys commonly used for the production of castings in Great Britain today.

Metallurgical Developments

Melting Practice—Aluminum and its alloys, when molten, readily absorb hydrogen, and considerable research has been carried out on preventive measures and on methods of degasification. Melting in a reducing atmosphere, which may contain free hydrogen, is a possible source of hydrogen pick-up, but recent investigations indicate that the main source of hydrogen is undoubtedly the presence of water vapor in the furnace atmosphere. It has been shown that the reaction between aluminum and water vapor, represented by $2 \text{ Al} + 3\text{H}_2\text{O} \rightarrow 3\text{H}_2 + \text{Al}_2\text{O}_3$, is irreversible at melting temperatures employed in normal foundry practice. Thus the reaction proceeds from left to right, with the formation of stable aluminum oxide, which is insoluble in the melt, and the liberation of hydrogen, which may be dissolved in the metal and subsequently liberated during solidification, forming gas porosity. Oxidation of the gassed melt therefore serves no useful purpose and merely forms more insoluble aluminum oxide, which is difficult to remove.

In view of the above consideration, probably the best way of minimizing hydrogen absorption in aluminum alloys is to maintain a thin unbroken oxide film on the surface of the molten metal during melting. Any factor which causes this oxide film to be disturbed, such as flame impingement, excessive stirring, etc., is likely to cause more trouble than incorrect furnace atmosphere. In this latter direction it is, of course, advisable, wherever possible, to employ a slightly oxidizing atmosphere. The amount of gas dissolved increases with rising temperature and it is

TABLE 2—MECHANICAL PROPERTIES OF ALUMINUM-ALLOY CASTINGS FOR GENERAL ENGINEERING PURPOSES (B.S. 1490 : 1949)

| Alloy | Specification | Stress, Min Tons p | Stress, Max er sq in. | | gation |
|------------------|--|--------------------------|-----------------------------|--------------|---------------|
| | | Sand Cast | Chill Cast | Sand Cast | Chill Cast |
| Al-Cu-Si-Zn | BS1490-LM-1-M | 8.0 | 10.0 | | _ |
| Al-Zn-Cu | BS1490-LM-3-M | 9.0 | - | - | - |
| Al-Si-Cu | BS1490-LM-4-M | 9.0 | 10.0 | 2 | 2 |
| Al-Mg | BS1490-LM-5-M | 9.0 | 11.0 | 3 | 5 |
| Al-Si (Alpax) | BS1490-LM-6-M | 10.5 | 12.0 | 5 | 7 |
| R.R.50 | BS1490-LM-7-M | 9.0 | 10.0 | 2 | 2 |
| Ceralumin B | BS1490-LM-7-P | 10.0 | 12.5 | 2 | 3 |
| Al-Si-Mg | BS1490-LM-8-W | 11.0 | 15.0 | 2.5 | 5 |
| 0 | BS1490-LM-8-WP | 15.0 | 17.0 | _ | 2.5 |
| Al-Si (Alpax | BS1490-LM-9-P | 11.0 | 15.0 | 1.5 | 2 |
| Beta) | BS1490-LM-9-WP | 15.5 | 19.0 | - | _ |
| Al-Mg | BS1490-LM-10-W | 16.0 | 18.0 | 7 | 12 |
| Al-Cu | BS1490-LM-11-W | 14.0 | 17.0 | 7 | 13 |
| | BS1490-LM-11-WP | 18.0 | 20.0 | 4 | 9 |
| | BS1490-LM-12-WP | | pecified | | |
| Al-Cu | AND A DO A | | Hardne | | -150 |
| Lo-Ex | BS1490-LM-13-WP | 11.0 | 16.0 | 233 100 | 130 |
| | BS1490-LM-13-WP (Special) | 9.0 | 13.0 | - | _ |
| Y alloy | BS1490-LM-14-M | 10.0 | 12.0 | - | - |
| | BS1490-LM-14-WP | 14.0 | 18.0 | _ | - |
| | BS1490-LM-14-WP (Special) | 12.0 | 15.0 | - | - |
| R.R.53B | BS1490-LM-15-WP | 18.0 | 21.0 | - | _ |
| Ceralumin C | | | | | |
| Al-Si-Cu | BS1490-LM-16-W | 11.0 | 13.0 | 2 | _ |
| | BS1490-LM-16-WP | 15.0 | 17.0 | - | - |
| Birmasil Special | BS1490-LM-17-M | 12.0 | 16.0 | 2 | 3 |
| Al-Si | BS1490-LM-18-M | 7.5 | 9.0 | 3 | 4 |
| Aeral A | BS1490-LM-19-W | 14.0 | 17.0 | 3 | 5 |
| Al-Si | BS1490-LM-20-M | 10.5 | 12.0 | 3.5 | 5 |
| Abbreviations: | M-as cast W-Solution treat | 1 | P—Aged P—Fully | | |

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important that the metal should not be heated above the normal temperature required to produce good castings.

Degasification—Gas-scavenging methods are widely employed for the removal of hydrogen from aluminum alloys. In Great Britain degassing is most commonly effected by the use of nitrogen or hexachloroethane (C2Cl6). The latter is an organic compound which evolves chlorine when heated to aluminum-melting temperatures, and consequently provides a convenient method of gas-scavenging. Flux manufacturers sell hexachloroethane, usually under a trade name, in the form of powder or tablets made up into units, weighed for various sized melts. The surface of the molten metal, at a temperature of between 700 to 750 C, is covered with a layer of suitable flux and the required amount of hexachloroethane is plunged to the bottom of the melt and held there for about 3 to 5 min, until no further gas bubbles are evolved. The evolution of chlorine from the degasser should not be too violent as small and evenly distributed bubbles promote more effective degassing.

After degassing the melt is preferably allowed to stand for 10 to 15 min before casting, while the flux is rabbled into the metal surface, prior to its removal, together with the dross, by means of a perforated hand ladle or scoop. The use of chlorine gas for degasification purposes is today being largely superseded by the more convenient hexachloroethane. With some alloys chlorine treatment has been found to increase grain size, but this can be easily corrected by the addition of suitable grain-refining elements, either after degassing or as a constituent of the flux. Treatment with chlorine, hexachloroethane, and fluxes containing chlorides also leads to a slight reduction in the magnesium content of the melt, which must be taken into account when melting complex alloys.

Removal of gases from aluminum alloys has also been effected by treatment with other volatile chlorides, such as titanium tetrachloride and boron trichloride. These compounds, in addition to removing hydrogen, promote grain refinement, due to the reduction of titanium and/or boron which is absorbed by the melt.

The British Non-Ferrous Metals Research Association have done a large amount of work over the past 20 years on the effect of gases on aluminum alloys, and have developed a degassing process, which combines treatment with fluxes and with an inert gas such as nitrogen. These developments are covered by British Patents Nos. 435104, 456462, and 569619.

In this process the nitrogen acts as a scavenger, while the flux serves to remove the oxide film which would hinder the escape of hydrogen from the melt. The fluxes employed consist of alkali chlorides and fluorides, a typical mixture being 50 parts potassium chloride, 40 parts sodium chloride, 10 parts sodium fluoride; the amount of flux used is 1 to 2 per cent of the weight of metal to be treated. The main disadvantage of degassing methods embodying the use of such fluxes is that the liquid flux attacks refractory crucibles. This has been effectively overcome by the latest process developed by The British Non-Ferrous Metals Research Association (British Patent No. 569,

619); see Fig. 16.

It will be observed that in this process the liquid flux is prevented from reaching the crucible walls by being confined within a removable graphite sleeve which dips into the molten metal. Agitation of the metal within the sleeve is effected by means of dry nitrogen passing through the lead-in tube. By this method degasification of melts weighing around 1,000 lb has been satisfactorily effected in about 12 min.

It is most important that all gases used for degasification purposes be dry. In this connection, high-pressure nitrogen cylinders can be employed, without the need for drying towers, provided that the gas pressure is not allowed to fall below 20 atmospheres. The minimum quantity of nitrogen required for treatment of aluminum and its alloys is about 25 cu ft per

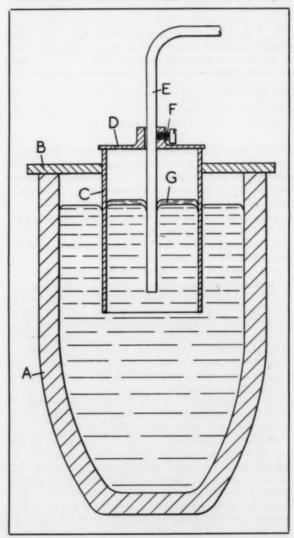
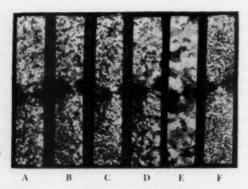


Fig. 16—Latest method of degassing aluminum alloys developed by The British Non-Ferrous Metals Research Association (B.P. 569,619). A.—Plumbago crucible. B.—Vented cover plate to hold inner plumbago sleeve C. D.—Vented plate covering inner sleeve and holding nitrogen delivery tube E. G.—Flux covering.



(Courtesy Murphy, Wells and Payne)

| | A | В | С | D | E | F |
|--|-----|-----|---------|-------|--------|------|
| Max Temp of Melt, °C | 720 | 740 | 750 | 760 | 800 | 8004 |
| Pouring Temp. °C | 720 | 720 | 720 | 720 | 720 | 720° |
| * Max temperature of me melted 730 C and poured | | | st into | chill | molds. | Re |

Fig. 17-Effect of melting temperature on grain size of aluminum alloy sand castings containing 2.5 Cu, 1.5 Ni, 1.2 Fe, 1.2 Si, 0.8 Mg.

ton of metal. Furthermore, the flow of gas should be controlled at such a rate as to cause the surface of the melt to roll gently. Violent agitation should be avoided and as mentioned previously, small and evenly distributed gas bubbles best promote degasification.

Fluxes—In the melting of aluminum alloys a flux serves three main purposes:—

1. It cleans the surface of the molten metal by removing oxide and dross;

2. It provides a protective covering during melting, to minimize formation of oxide, etc., and

3. Under certain conditions, it acts as a degasser. Whether or not it is necessary to use a flux depends upon various factors, e.g. composition of the alloy, melting conditions and nature of the furnace charge. Alloys containing more than 2 per cent magnesium tend to form dross and absorb hydrogen much more rapidly than do lower-magnesium or magnesium-free alloys. A protective low-melting point covering flux, usually composed of fluorides and chlorides of magnesium and potassium, is therefore recommended when melting alloys of high magnesium content.

For alloys containing little or no magnesium the use of a flux is not essential, and need only be employed when melting conditions are excessively oxidizing in character, when the furnace is of a type which permits the flame and products of combustion to come into direct contact with the molten metal, or if the charge contains considerable percentages of scrap. In the case of these latter alloys fluxes are generally composed of sodium or potassium chlorides and sodium fluoride, a typical mixture being two parts by weight of sodium chloride and one part of sodium fluoride. Chemicals of commercial quality are satisfactory but it is important that they be perfectly dry when added to the metal, as the use of damp flux is likely to result in gassy castings.

The procedure commonly employed in using a flux is to raise the metal temperature to 700 to 750 C and then spread the dry flux evenly on top of the metal surface, in an amount varying from 0.5 to 2 per cent of the metal charge-weight, according to individual conditions. The flux is then gently pushed, or rabbled, under the surface of the molten metal (by means of an iron skimmer or plunger) until it is seen to be fused. The metal is then stirred, the dross removed by skimming and the metal cooled to the required pouring temperature. Care should be taken to avoid excessive agitation of the molten metal.

It is now generally appreciated that fluxes containing fluorides are the most effective degassers and that changes in chemical composition of aluminum alloys can occur as a result of flux treatment. Murphy, Wells and Payne⁶ have shown that a serious loss of magnesium can arise from a repetition of the sodium chloride-sodium fluoride flux treatment. It is clear, therefore, that if for any reason a melt of an aluminum alloy containing magnesium is treated more than once, with a flux of the type stated, an addition of magnesium should be made to compensate for the loss due to the action of the flux. The same consideration should be borne in mind if remelted runners and risers constitute a large proportion of the charge.

Grain Refinement-Murphy, Wells and Payne⁶ have also investigated the effect of melting temperature on the grain size of sand castings and have found that this is considerably increased if the temperature of the melt exceeds 750 C. This effect is quite distinct from the influence of pouring temperature. Figure 17, reproduced by courtesy of the authors, illustrates the phenomenon in an alloy having the composition: Copper 2.5, nickel 1.5, iron 1.2, silicon 1.2, magnesium 0.8 per cent, aluminum remainder. The macro-etched sections shown were taken from sand-cast plates 6 in. x 2 in. x 3/8 in., all of which were cast at a temperature of 720 C after heating the melt to the various temperatures indicated. The sudden increase in grain size when the melting temperature passes 750 C is clearly seen. The authors suggest that while more experimental evidence would be required before a satisfactory explanation of this effect could be given, it may be connected with the formation of nuclei in the molten metal below a temperature of 750 C, and their disappearance at higher temperatures.

It is also emphasized that in any investigation of the relation between melting conditions and grain size, it is necessary to guard against the spurious effects which can be introduced by dissolved gas. The escape of gas from the cooling metal, as it approaches the freezing point, causes agitation, which in itself produces a certain degree of grain refinement. It is therefore essential to provide for this factor, by ensuring that the molten metal is substantially free from dissolved gas, when the influence of other variables is to be studied.

The founding properties of aluminum alloys are improved when the metal is intrinsically fine-grained, since the risk of hot-tearing, cracking in the mold, and the tendency towards shrinkage porosity are reduced. The latter advantage appears to result from

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the better feeding characteristics possessed by metal solidifying with a fine grain. This is especially important in high-strength aluminum alloys, which are always characterized by a large contraction of volume in the transition from the molten to solid state. Furthermore, in these alloys heat-treatment by quenching from 500 to 530 C is also often involved, and here again a small grain size reduces the danger of cracking. When considering the mechanical properties of actual castings, the most important effect of grain refinement is not in conferring high strength values, but in the promotion of more uniform properties throughout the casting. This latter point is particularly important in the production of castings of guaranteed properties which must withstand severe service conditions such as arise in modern automobile and aircraft

Although several different methods have been put forward for promoting fine grain in aluminum alloy castings, the most easily applied method, from the practical point of view, is the introduction, into the alloy, of small amounts of a suitable grain-refining element such as titanium or niobium. Additions of up to 0.3 per cent titanium have been used in Great Britain for this purpose for many years; the amount of titanium necessary to ensure a consistent fine grain size, for a wide range of alloys, appears to be in the order of 0.15 per cent.

More recently, following the work of Murphy, Wells and Payne,⁶ it has been shown that additions of up to 0.1 per cent niobium also promote grain refinement, as shown by Fig. 18 and 19. In many alloys a grain-refining effect is noticeable with as little as 0.02 per cent niobium, and the degree of refinement increases with increasing niobium additions until a maximum of 0.10 per cent is reached.

Fig. 18—Macrostructure of aluminum alloy containing 2.5 Cu, 1.5 Ni, 1.2 Fe, 1.2 Si, 0.8 Mg, Nb—nil, poured at 800 C into a dry-sand mold to produce a cylindrical casting 3 in. diam x 3 in. high.

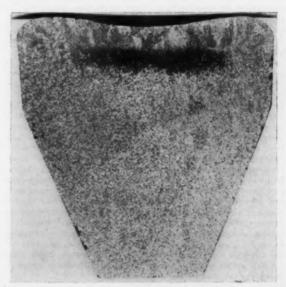
It should, however, be noted that the amount of titanium and niobium required to obtain maximum grain refinement is affected by the composition of the base alloy. It is not advisable to add more grain-refining element than is necessary, since loss of ductility may result. Furthermore, it is preferable to use an addition alloy, containing about 5 per cent of the grain-refining element, with an iron content as low as possible, to avoid trouble when producing aluminum alloys sensitive to iron "pick-up."

Some measure of grain refinement can also be obtained (if necessary supplementing the effect of the grain refiner already present) by the addition of boron. It is usually added by means of a sodium chloride-fluoride flux containing a boron salt, such as fused borax (Na₂ B₄ O₇). Boron is, however, inclined to be rather variable in its action and when reliable control of grain size is required the use of titanium or niobium is generally to be preferred.

The presence of boron in aluminum alloys also tends to promote gas absorption, both during melting and in the mold, and the resulting decrease in the soundness of the casting offsets the beneficial effect of grain refinement. Proprietary degassers of the hexachloroethane type are also obtainable containing a proportion of boron salts; these act as both degassers and grain refiners.

The latest work on the important subject of grain refinement in aluminum alloys has emanated from The British Non-Ferrous Metals Research Association. Cibula and Ruddle⁷ recently investigated the effect of grain size on the tensile properties of high-strength cast aluminum alloys, and as a result of this work, put forward the following conclusions:—

1. "Grain refinement modifies the form of intergranular shrinkage cavities in cast aluminum alloys and thereby improves



(Courtesy Murphy, Wells and Payne)

Fig. 19-Effect of 0.1 Nb on grain size of aluminum alloy test block cast under the same conditions as outlined in Fig. 18.

tensile properties, the effect being small when the alloy contains brittle intergranular constituents, but very marked when such constituents are absent or present only in small amounts."

2. "In test bars of a high-strength heat-treated aluminum-4.5 per cent copper alloy (B.S.1490-LM-11) a change in grain size from 2 m/m to 0.2 m/m is associated with increases in strength from 14 to 19 to 23 tons per sq in., and in elongation from about 2.5 to over 10 per cent, these being primarily due to associated changes in the form of the porosity in the cast material."

3. "Grain coarsening of the aluminum-4.5 per cent copper alloy becomes marked when the metal is heated to temperatures exceeding 720 C; it can be minimized (a) by adding to the melt a suitably prepared aluminum-titanium-carbon alloy and (b) by adding boron to the melt. Of these remedies the first is preferable because the presence of boron promotes gas absorption in the mold and the resulting decrease in the soundness of the casting partly offsets the beneficial effect of grain-refinement."

Eborall,⁸ in a recent paper, studied the effect of titanium, zirconium, niobium, molybdenum, chromium and boron, on the grain refinement of very pure aluminum, and found that titanium and zirconium were the most effective grain-refining agents. It is emphasized, however, that this conclusion may not necessarily apply to aluminum or aluminum alloys of ordinary commercial purity.

Cibula⁹ has investigated the mechanism of grain refinement of sand-castings in aluminum alloys, with most interesting results. He concludes that grainrefinement in any given case is due to one or other of

the following reasons:-

A. "Restriction of crystal growth by concentration gradients, associated with increased undercooling, at the surface of the casting and, more particularly, with the spread of undercooling into the interior of the casting, the latter condition producing an equiaxial structure. The grain refinement produced by this mechanism in aluminum alloys with copper, nickel, lead, etc., is relatively slight until a large proportion of the alloying element is present, and is never very marked."

B. "The presence of nuclei upon which the aluminum solid solution crystallizes easily, this being associated with the virtual suppression of undercooling. This mechanism alone, e.g. in aluminum alloyed with very small proportions of titanium, will only refine a coarse columnar to a much finer but still columnar structure, and will not produce the equiaxed structures char-

acteristic of marked grain-refinement.'

C. "The combined effects of concentration gradients and of nuclei, grain refinement then being very marked and again associated with the virtual suppression of undercooling, although it is possible that very slight undercooling occurs and that it is the function of the concentration gradients to produce this. This marked grain refinement occurs in aluminum alloyed with substantial proportions of titanium, zirconium, vanadium, molybdenum, or tungsten, where the alloying element provides both the concentration gradient and the nuclei. In some other alloys of aluminum with powerful grain refining elements, notably niobium and boron, the composition range of solid solution formation is very restricted, primary particles of the respective compounds with aluminum appearing in alloys containing only 0.04 per cent boron or 0.04 per cent niobium. In these systems therefore, the alloying element fails to produce the concentration gradient effect and maximum refinement is only obtained if another element, e.g. copper, is added to provide the restriction of crystal growth. Thus the theory satisfactorily accounts for the fact that boron and niobium have a relatively slight grain refining action when added to super-pure aluminum although they refine aluminum-base alloys markedly."

So far as the identity of nuclei are concerned, Cibula concludes that these are carbides of the grain refining metals. These carbides contain carbon atoms in interstitial positions, in simple metal lattice structures of which the dimensions are similar to those of the alumi-

num lattice. Certain transition metals, notably chromium and manganese, form complex carbides, the structures of which differ markedly from that of aluminum, and these transition elements do not cause grain refinement. The importance of this latest work from The British Non-Ferrous Metals Research Association needs no emphasis; it effectively fills many gaps in our existing knowledge and provides a reasonable explanation of the general mechanism of grain-refinement in aluminum-base alloys.

Production Methods

Melting Furnaces—Satisfactory results are obtained from a wide variety of furnaces and the type ultimately employed depends largely upon local conditions, such as the relative cost of oil, gas, coke and electricity, or the nature and size of castings being produced. For the direct production of castings British foundries specializing in the production of aluminum alloys tends to prefer the use of oil- or gas-fired crucible furnaces of the tilting or bale-out type, similar to those described in the Exchange Paper by Vickers, ¹⁰ given to The American Foundrymen's Society in 1944. Both refractory and cast-iron crucibles are employed. Cast iron presents certain advantages over refractory materials for crucibles, as it has better thermal conductivity, superior strength and lower cost.

On the other hand, certain precautions, unnecessary in the case of refractory crucibles, have to be observed with iron melting vessels, if contamination of the aluminum alloy with iron is to be avoided. Washes consisting of whitening 10 lb, sodium silicate 6 oz, water 5 gal, or china clay 10 lb, sodium silicate 1 lb, water 2 gal, applied by brushing to the inside of the crucible, have been found effective for furnaces which are completely emptied after each melt. It is necessary to renew the wash daily, and if this treatment is carried out conscientiously no appreciable pick-up of

iron occurs

Large oil-fired open-hearth furnaces are used for bulk-melting heats of 2 to 10 tons, such as are required in the production of ingots, and in some cases for supplying molten metal to holding furnaces, as used in die-casting. With direct flame heating, however, there is always an increased danger of gas absorption, particularly if the flame impinges on the metal, or the surface of the metal is disturbed by puddling, to effect complete mixing. Many of the difficulties associated with the open-hearth furnace are overcome by the use of the rotary cylindrical type, and this design is now being increasingly employed.

From the point of view of metal quality and ease of operation, the low-frequency electric induction furnace is very attractive, especially where continuous operation is required. Several 100 kw furnaces of 1300 lb capacity have been installed in Great Britain within the last ten years, and the results obtained have been described by Devereux.¹¹ While the initial cost of such a furnace is high, this is offset, in comparison with an oil-fired crucible furnace of similar capacity, by much lower metal losses and maintenance charges (in the absence of crucibles), resulting in a reduction of running costs by 29 per cent.

The constant movement of the metal in an induc-

Fig dies

tion furnace means that a fresh surface is being continually exposed to the atmosphere, and unless precautions are taken, excessive losses by oxidation will occur. The use of a suitable covering flux, or an inert atmosphere such as nitrogen, will prevent this effect, and in practice melting losses can be reduced to approximately one-third of those obtained from an oil-fired crucible furnace. Further advantages are that the foundry is kept clear of fumes and dirt, and fatigue of furnace operators is noticeably reduced, leading to increased production.

Pouring and Running Methods—Refractory coatings applied to iron ladles, melting pots and furnace tools such as stirrers, etc., must be thoroughly dried (by heating to a dull red heat) before use, otherwise gas porosity may be present in the castings. In the production of sand castings of medium and heavy section, particularly those which must withstand severe stresses in service, it is also important to ensure that the refractory linings of large ladles and mold-pouring basins are as free as possible from moisture. It is not uncommon for foundries producing such castings to fit sheet-steel linings to large ladles and even to line

pouring basins with asbestos paper, in order to minimize gas absorption.

Aluminum alloys are prone to oxide and dross formation, and it is important to avoid turbulence when pouring and running castings, especially in alloys containing substantial percentages of magnesium. In general, it would appear that multiple gating systems are to be preferred, as these promote more uniform solidification and minimize such defects as hot tears, shrinkage cavities, oxide seams, cold laps, etc. Furthermore, it is important to use choke gates, as if any dross enters the mold this will not rise to the top parts of the casting, due to the fact that it has practically the same specific gravity as the metal. Extensive use must be made of chills and heavy feeding risers.

It is difficult to generalize on the subject of running methods, since so much depends on the particular design of casting being produced and the service it has to withstand. However, further study of typical methods used by British founders, as illustrated in Fig. 20 to 24, may assist in this direction. It should also be pointed out that most British founders, particularly those making aluminum-alloy aero-engine cast-

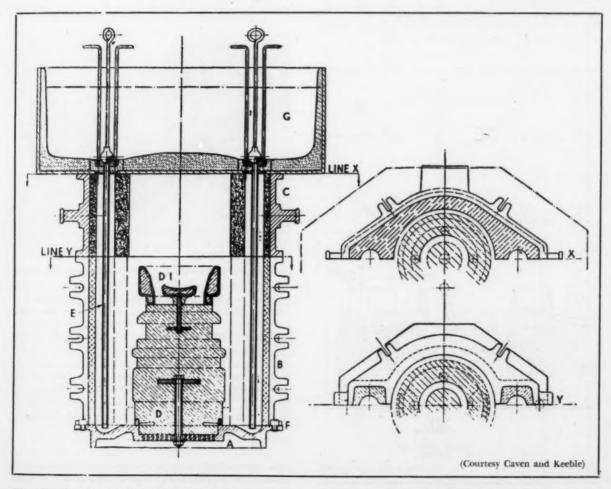


Fig. 20—Method employed for production of large diesel engine piston. A.—Cast iron base plate. B.—Two-piece cast iron chill mold. C.—Sand-lined feed-

ing head. D and D1-Sand cores. E.-Steel runner tubes. F.-Locating dowels. G.-Stopper-controlled pouring basin.



(Courtesy Caven and Keeble)

Fig. 21-Large spinning-chuck casting, 7 ft in diam and weighing 4000 lb as cast.

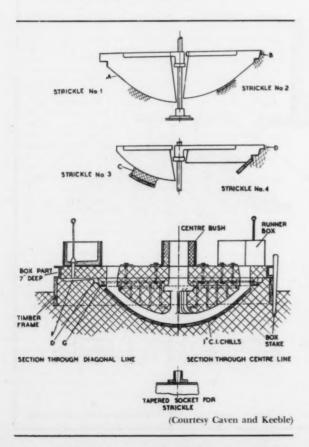
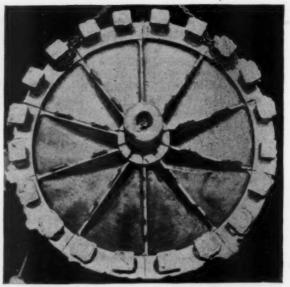


Fig. 23-Molding methods employed for production of large spinning-chuck casting.



(Courtesy Caven and Keeble)

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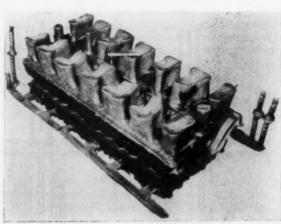
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Fig. 22-Plan view of large spinning-chuck casting.



(Courtesy High Duty Alloys Ltd.)

Fig. 24—Method of running aero-engine cylinder block cast in Hiduminium R.R. 50 (Sand casting). In the running methods finally adopted for production two sets of five main down runners, 7/8-in. diam, were employed instead of the double down runners shown in this photo. The down runners were spaced along the two horizontal runners, each 48 in. x 13/4 in. x 11/8 in. Each ingate has a section of 13/4 in. x 11/4 in. Furthermore the large risers were ultimately extended to the extreme edge of the casting and measure 5 in. x 41/4 in. x 2 in. End risers are 5 in. x 21/2 in. diam. Castings made before these modifications were effected were drawn.

ings, operate experimental foundries in which suitable production methods are first investigated, prior to mass production, in order to ensure that satisfactory castings are made with the minimum of wasters. This important problem of correlating foundry practice with casting quality has been adequately covered in a

recent paper by Warrington.14

Gravity Die-(Permanent-Mold)-Castings—Approximately 51 per cent of the aluminum-alloy castings produced today in Great Britain are made by gravity diecasting methods. By comparison with sand casting this process has the following advantages:—

(a) Improved mechanical properties in the casting.(b) Smoother finish and closer dimensional accu-

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(c) Greater possible speed of production.

(d) Higher yield of sound castings.

Production methods employed have been described in detail in recent papers published by Vickers, ¹⁰ Martin, ¹² and Caven and Keeble.² Probably the largest gravity die-casting so far produced, in terms of surface area and thinness of section as distinct from weight, is a double-draining-board type of sink unit described in the paper by Martin, and reproduced in Fig. 9. This die-casting, measuring 63 in. x 21 in. x ³/₁₆ in. thick, was cast in aluminum alloy containing 10.0 to 13.0 per cent silicon, suitably modified with sodium.

Large diesel-engine pistons in Y-alloy (see Fig. 14), 22 in. diam x 33 in. long, and having a weight of nearly 800 lb as cast, are regularly produced by gravity die-casting, using sand cores. An interesting and successful method of making such castings has been outlined by Caven and Keeble² as shown in Fig. 20. One of the specially noteworthy features of this method is the use of $\frac{7}{8}$ in. diam steel runner tubes, within the two main down-gates, to facilitate pouring without excessive turbulence. Mold and cores are heated to 200 C prior to pouring at a temperature of 710 C following which the steel runner tubes are withdrawn and the runner box is removed. This operation exposes the feeding head and permits the addition of feed metal at 750 C, to compensate for shrinkage. (To secure adequate soundness in large piston castings the metal must be free from gas, and grain refinement must be complete.)

Special methods must be employed for the heattreatment of large Y-alloy castings, in order to avoid distortion and residual stresses. In the case of the large piston just described, the recommended heattreatment is as follows:—

"Load into a cold furnace and raise slowly to 500 to 515 C after soaking for about 5 hr at temperatures of 150 C, 300 C, and 450 C. After maximum temperature has been reached maintain for 24 to 30 hr, then drop the temperature to about 485 C for about 1 hr. Quench in warm oil and withdraw after ½ hr to drain, keeping free from draughts. As soon as possible recharge casting into furnace at about 150 C. After a 3-hr soak raise temperature to 275 C and heat for a further 3 hr followed by cooling in still air. This type of treatment produces a stable condition, free from residual stresses, and the mechanical properties of standard D.T.D. test bars will be around 12 to 13 tons per sq in. with a Brinell hardness value of 75."

The evacuated-die process, first patented by Bruneau Freres in France, is also operated in Great Britain, for the production of heavily finned cylinder head castings similar to that shown in Fig. 10. In this process the die is built up, where the fins occur, from banks of blades. As the metal rises in the die the air between the blades is withdrawn, by means of a vacuum pump,

TABLE 3-ALUMINUM ALLOYS USED FOR PRESSURE DIE-CASTING

| | | | | | | | | | | | 0.10/ | Viold | | P. I. | 11 11 di |
|-----------|------------------------------|--------|--------|---------|------|--------|--------|---------|-----|---------------------------|--------------|--|--------|----------|----------|
| Alloy | Specifications | Al | - C | S | Mg | Fe | Mn | Z | II | Other | Proof Stress | Point | Stress | Elong. % | Hardness |
| | | | | | | | | | | Elements | sq in. | sq in. | sq in. | 2 III. | NO. |
| Aluminum- | BS702 | | | | | | | | | Zn 0.1 max | | | | | |
| Silicon | BS2.L.33 | Bal | 0.1 | 10.0 to | 0.1 | 9.0 | 0.5 | 0.1 | - | Pb 0.1 max | XC. 4 | and the same of th | 12.0 | 7.0 | 60-65 |
| | BSSTA7/AC.6 BS1490-LM-6-M | | max | 13.0 | max | тах | max | max | | Sn 0.05 max | | | | | |
| Aluminum- | | | | | | | | | | Zn 0.2 max | | | | | |
| Silicon | BS1490-LM-20-M | Bal | 0.4 | 10.0 to | 0.15 | 0.7 | 0.5 | 0.1 | 0.2 | Pb 0.1 max | 4.5 | - | 12.0 | 5.0 | 60-65 |
| Aluminum- | | | max | 13.0 | max | max | max | max | max | Sn 0.05 max | | | | | |
| Silicon- | BSSTA.7/AC.2 | | 0.7 to | 9.0 to | 0.3 | 1.0 | 0.5 | 0.5 | 0.2 | Zn 1.2 max | 1 | 1 | 9.5 | 1 | 75-85 |
| Copper | BS1490-LM-2-M | Bal | 2.5 | 11.5 | max | max | max | max | max | Pb 0.3 max | | | | | |
| | | | | | | | | | | Sn 0.2 max | | | | | |
| Aluminum- | DTD424 | | | | | 1.3 | max | | | Zn 0.5 max | | | | | |
| Silicon- | BSSTA7/AC.4 | Bal | 2.0 to | 4.0 to | 0.15 | 8.0 | 0.3 to | | 0.2 | Pb 0.1 max | 5.5 | 1 | 10.0 | 2.0 | 08-09 |
| Copper | BS1490-LM-4-M | | 4.0 | 0.9 | max | max | 0.7 | | max | Sn 0.05 max | | | | | |
| Aluminum- | | Bal | 0.1 | 4.5 to | 0.10 | 9.0 | 0.5 | | 1 | Zn 0.1 max | 1 | 1 | 0.6 | 4.0 | 1 |
| Silicon | BS1490-LM-18-M | | max | 0.9 | max | max | max | max | | Pb 0.1 max Sn 0.05 max | | | | | |
| P.2 | | Bal | 3.0 to | 4.0 to | 0.5 | 2.0 to | 0.5 | 1.75 to | 1 | 1 | 1 | 7.0 to | 9.5 to | 0.5 to | 101-69 |
| | | 11 1 1 | 4.5 | 5.0 | max | 4.0 | max | 2.50 | | | | 7. | 12.25 | 2.0 | |

which assists the molten metal to flow to the extremity of the fin without tendency for misruns or blow holes

due to entrapped air.

Sand Castings—Sand castings account for approximately 35 per cent of the total aluminum-alloy castings production; the castings made range from ounces up to 4,000 lb in weight. The largest so far made in Great Britain is believed to be the spinning-chuck casting, described by Caven and Keeble, ¹³ and shown in Fig. 21 to 22. This saucer-shaped casting measures 7 ft in diam, has a section varying from 31/2 to 5 in., and weighed 4,000 lb as cast. It is interesting to note that the pattern equipment was arranged in the form of four strickles, (1)—A, to form mold and position the chills; (2)—B, to form core print for the center core; (3)—C, to strickle facing of sand to represent wall thickness, and (4)—D, to form the ring runner. These and the mold arrangement are shown in Fig. 23.

The casting was run from four pouring basins through the main ingates F connected to ring runner D. Twenty small finger-type ingates G linked the ring runner with the mold, to ensure even distribution of the molten metal without undue turbulence. Over each of the ingates G was a 6 x 4-in. riser and a 10-in. diam riser, twice the length of the rim risers, was located on the central boss. The alloy used contained silicon 3.0 to 6.0, copper 2.0 to 4.0, iron 0.8, manganese 0.7, nickel 0.35, titanium 0.2, magnesium 0.15 per cent, remainder aluminum. The metal was poured at 690 C and feed metal at 750 C was added to compensate for shrinkage in the various risers which were rod fed.

The production of large sand castings in aluminum alloys of the size and weight just mentioned is the exception rather than the rule. Most of the castings made at the present time are of small and medium size, similar to those illustrated elsewhere in this paper. Conditions in Great Britain, in general, are not economically suitable for the installation of fully mechanized plants. Repetition lines are few and far between, with the result that most foundries have been designed to meet a semi-repetition throughput. They are, however, provided with all mechanical aids such as sand-preparation and distribution system, molding machines, sand-slingers, roller conveyors, etc.

Pressure Die Castings-Production of pressure die castings amounts to approximately 14 per cent of the total aluminum-alloy castings manufactured, and is regulated by the weight and size of the casting, its intricacy and the quantity required. The initial cost of a die is roughly three times that of a gravity die and it is desirable to have in mind quantities of not less than 50,000 when considering this method of production. Pressure die-casting machines, generally of either the hot- or cold-chamber type, have a casting capacity, including slug and gates, up to about 20 lb and are suitable for producing castings of even wallsection not exceeding 0.185 in. thick, and up to 300 sq in. in projected area. Only those alloys having a high degree of fluidity, similar to those listed in Table 3, can be satisfactorily employed for the production of pressure die-castings. Figure 11 illustrates typical castings produced by this method in Great Britain.

Certain foundries have developed a low-pressure

TABLE 4-STANDARD HEAT TREATMENT SCHEDULES FOR VARIOUS ALLOYS

| | | Solution Trea | atment | | Aging Treatr | nent |
|-----------------------------|----------------|--------------------|---------------------------------------|-----------------------------|--------------------|------------------------------|
| Alloy | Temp. °C | Time, Hr | Quenching Medium | Temp. °C | Time, Hr | Quenching Medium |
| R.R.50 | None r | equired | | 155–170 | 8–16 | Hot water or cool in air |
| Ceralumin B | None re | equired | | 165–175 | 8-16 | Oil, water or cool in air |
| Al-Si-Mg (BS1490-LM-8-W) | 520-540 | 5 | Boiling water or oil | 155–175 or | 4-14 | Oil, water or cool in air |
| , | | | 1 | Boiling water or Room | 6 5 days | |
| Alpax Beta | 520-535 | 2-4 | Water | 150-175 | 16 | Oil, water or cool in air |
| Al-Mg (BS1490-LM-10-W) | 425-435 | 16 | Oil below 150 C | None req | uired | |
| Al-Cu (BS1490-LM-11-W) | 525-545 | 12-16 | Water | 120-170 or | 12-14 | Air |
| 201100 2011 11 11) | | | | Room | 5 days | |
| .o-Ex | 520-525 520 | 2-4 6 | Water Boiling | 150-175 | 4-14 | Oil, water or cool in air |
| alloy | | | water | Room | 5 days | _ |
| | | | | Boiling | 2-3 | Cool in air |
| R.R.53B | 530-545 | 6-16 | Boiling water, oil or air blast | 150–180 | 10-30 | Water or cool in air |
| Ceralumin C | 520-530 | 4-6 | Oil or water | 165–175 | 8-16 | Oil, water or air-cooled |
| Ceralumin A.S.M. | 525-535 | Not less than 2 | Water at 50-70 C | Boiling water | Not less than 1 | Cool in air |

Fig.

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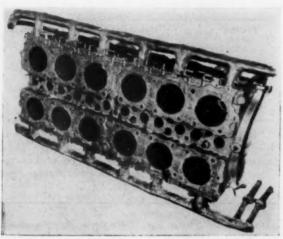
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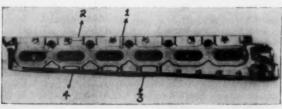
process, in which the molten metal is blown into the die by means of compressed gas or air. This process has certain advantages over the more usual high-pressure die-casting methods, inasmuch as larger castings of heavier and variable section can be produced to close dimensional tolerances and with excellent density, detail and finish. Furthermore, the low-pressure process can be more or less successfully operated with any type of aluminum-base alloy and castings are not so liable to contain defects due to entrapped air. Representative die-castings made by the low pressure process are shown in Fig. 12.

Centrifugal Castings—Centrifugal casting of aluminum alloys has not so far been considered as of practical importance, because it was believed that the speeds required would be too high. However, following the work of Northcott and Lee, 15 it is now apparent that by suitable attention to casting design, in addition to molding and pouring technique, rotational speeds required to produce improved properties can be brought into the practicable range. In this investigation 10 and 11-in. diam spoked wheels, in various aluminum alloys, were successfully cast centrifugally in sand molds rotated on a vertical axis. In general, it was



(Courtesy High Duty Alloys Ltd.)

Fig. 25—Underside view of cylinder block shown in Fig. 24. Cylinder bores and base of casting are chilled. Pouring temperature 750 C. In view of the large number of cores employed in the manufacture of this casting careful attention must be given to core quality and venting in order to minimize porosity and misruns.



(Courtesy Rolls-Royce Ltd.)

Fig. 26-Position of test bars cut from sand cast R.R. 50 Merlin aero-engine cylinder head.

found that the highest practicable speeds of rotation are desirable for aluminum-alloy castings, since the internal centrifugal pressure is naturally low for metal of low density. For the wheel patterns employed, a minimum peripheral speed of 2600 fpm (1000 rpm) was desirable, producing a pressure of 32 psi on the mold wall.

Heat Treatment-Mechanical properties of most aluminum-alloy castings can be improved by heattreatment, and full advantage is taken of this fact by founders producing castings for specialized service conditions. Different alloys require different heattreatments; those normally applied in Great Britain are indicated in Table 4. In most instances solution treatment is carried out in air-circulation pit-type electric furnaces equipped with the necessary quenching facilities. Precipitation or aging treatment is conducted in low-temperature ovens heated by electricity or gas. Automatic temperature control is fitted to all furnaces and ovens, in conjunction with recording instruments and alarm devices which operate if the temperature becomes excessive. In many instances degreasing plant, for treatment of castings after oil-quenching, is included as part of the equipment in the heat-treatment department.

In the heat-treatment of light and intricate castings, care must be taken to avoid distortion, and in some



(Courtesy Rolls-Royce Ltd.)

Fig. 27-Position of test bars cut from sand cast R.R.50 Merlin aero-engine cylinder head.



(Courtesy J. Stone & Co. Ltd.)

Fig. 28-Position of test bars cut from aircraft tail wheel landing fork cast in Ceralumin C and A.S.M.

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cases it may be necessary to heat-treat on formers. It is interesting to note that the aircraft windscreen frame casting in Ceralumin A.S.M. illustrated in Fig. 6 was heat-treated in this way.

Casting Quality—Technical control has always been a keynote of the aluminum alloy foundry industry in Great Britain. After satisfactory production methods have been determined in the experimental foundry, routine check tests on all important components are made, using radiographic, metallographic, pressure, and mechanical testing methods, on representative castings selected from production batches. The results obtained on just a few of these tests, shown in Fig. 24 to 28 and Tables 5 to 7, demonstrate that British aluminum-alloy castings deserve a high reputation for reliability, quality and craftsmanship.

Acknowledgment

In conclusion, the author acknowledges his indebtedness to The Mond Nickel Co., Ltd. for permission to prepare this paper and to High Duty Alloys, Ltd., Rolls-Royce Ltd., J. Stone and Co., Ltd., William Mills, Ltd., Northern Aluminum Co. Ltd., John Dale, Ltd., Metal Castings, Ltd., The Non-Ferrous Die Casting Co. Ltd., Birmingham Aluminum Castings (1903) Co. Ltd., The Aluminum Development Association and The British Non-Ferrous Metals Research Association for their most helpful co-operation.

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TABLE 5-PROPERTIES OF TEST BARS CUT FROM R.R.50 SAND CAST AND HEAT TREATED AERO-ENGINE

CYLINDER BLOCK Shown in Fig. 24 and 25 (Courtesy High Duty Alloys, Ltd.)

| Test Bar Mark | Yield Point, Tons per sq in. | Max Stress, Tons per sq in. | Elongation Per Cent |
|------------------|---------------------------------|--------------------------------|------------------------|
| 1 | 8.5 | 10.4 | 3.0 |
| . 2 | 9.7 | 12.6 | 7.0 |
| 3 | 9.6 | 10.7 | 2.0 |
| 4 | 8.2 | 9.2 | 2.0 |
| 5 | 8.4 | 8.7 | 2.0 |
| 6 | 8.0 | 8.4 | 3.0 |
| 7 | 8.8 | 9.5 | 2.0 |
| 8 | 7.3 | 8.4 | 4.0 |
| 9 | 7.8 | 9.5 | 4.0 |
| 11 | 10.4 | 12.2 | 3.0 |
| 12 | 8.5 | 9.7 | 4.0 |
| 13 | 8.5 | 9.3 | 3.0 |
| 14 | 8.8 | 9.6 | 3.0 |
| 15 | 7.7 | 8.6 | 2.0 |
| 17 | 8.3 | 9.3 | 3.0 |
| 18 | 7.2 | 9.7 | 3.0 |
| 19 | 8.9 | 10.3 | 4.0 |
| 20 | 8.7 | 8.9 | 4.0 |

TABLE 6—PROPERTIES OF TEST BARS CUT FROM R.R.50
SAND CAST AND HEAT TREATED MERLIN AERO-ENGINE
Cylinder Head shown in Fig. 26 and 27

| Test Bar Mark | Max. Stress Tons per sq in. | Elongation Per Cent |
|------------------|--------------------------------|------------------------|
| 1 | 11.9 | 4.0 |
| 2 | 11.8 | 3.0 |
| 3 | 10.4 | 4.0 |
| 4 | 10.5 | 3.0 |
| 5 | 10.9 | 3.0 |
| 6 | 10.6 | 1.0 |

TABLE 7-PROPERTIES OF TEST BARS CUT FROM CERA-LUMIN C AND A.S.M. SAND CAST AND HEAT TREATED AIRCRAFT TAIL WHEEL LANDING FORKS

SHOWN IN FIG. 28
(Courtesy J. Stone & Co., Ltd.)

| Test Bar Mark | 0.1 Per Cent Proof Stress Tons per sq in. | Max Stress Tons per sq in. | Elongation Per Cent |
|------------------|---|-------------------------------|------------------------------|
| Ceralumin (| C | | |
| FA | 19.6 | 21.9 | 0.8 |
| FB | 19.6 | 20.4 | 0.8 |
| FC | | 17.8 | 0.5 |
| FD | _ | 23.6 | 1.3 |
| FF | - | 24.8 | 1.1 |
| FH | - | 21.6 | Broke outside gauge marks |
| FK | 20.4 | 24.4 | 1.3 |
| Ceralumin A | A.S.M. | | |
| FA | 12.4 | 16.84 | 4.0 |
| FB | 11.2 | 16.00 | 4.0 |
| FC | 10.98 | 15.55 | 6.0 |
| FD | 11.40 | 13.64 | 2.0 |
| FE | 11.90 | 15.12 | 3.0 |
| FF | 11.76 | 17.60 | 6.0 |
| FG | 11.92 | 17.68 | 5.0 |
| FH | | 12.85 | 2.5 |
| FJ | 11.58 | 14.40 | 3.5 |
| FK | 11.72 | 17.00 | 5.0 |
| FL | 11.60 | 15.64 | 3.5 |
| FM | - | 19.00 | 8.5 |
| FN | | 16.84 | 7.0 |

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DISCUSSION

Chairman: S. C. Massari, American Foundrymen's Society,

Co-Chairman: HIRAM BROWN, Solar Aircraft Corp., Des Moines, Iowa

MEMBER: Is the practice of using niobium quite standard and if so, is there some danger of scarcity of niobium? Is it introduced as an alloy, the element, or perhaps the double fluoride?

Mr. Hubson: While titanium is usually employed for grain refining purposes at least one large British aluminum alloy founder has standardized on the use of niobium, and a considerable tonnage of castings containing this element have been produced over a period of many years.

Niobium is added, as mentioned in the paper, by means of a special addition alloy or through the use of pre-alloyed ingots. The use of niobium is covered by patents and the firm owning these usually arranges to supply licensees with either the addition alloy or with pre-alloyed ingots suitable for casting purposes.

So far as titanium is concerned the original patents have expired and there are consequently now no restrictions in Great Britain to its use for grain refining purposes

CO-CHAIRMAN BROWN: The columbium situation is bad in U.S. because Britain controls it. They have all of the columbium in South Africa and we are not getting it here. So far as we know, there is no actual shortage today any more than there was, except that we do not get it here.

(Note: In U.S.A. niobium is called columbium.)

D. L. Colwell: 1 I enjoyed the paper very much because it summarized much of what we found a year ago. I was one of a committee of two to visit Europe and check into the uses of aluminum under the Marshall Plan. We provided funds for the purchase of a considerable portion of aluminum used in British production. They produce about 30,000 metric tons in Scotland and they were using over 200,000 metric tons. They have their goal set at over 300,000 metric tons per year. This gives a background to some of the uses they have for aluminum which to us seem illogical.

England is making a tremendous drive to increase exports, and they have selected aluminum as one of the raw materials for manufacture. There is therefore a tremendous pressure on the part of the Socialist Government for the use of aluminum as a raw material rather than other materials. In places where we might use sheet steel, for instance, or stampings, they would use aluminum castings. The picture is also complicated by the fact that their normal production quantities are nowhere near ours, and tooling up for a complicated stamping might be too expensive for the benefits involved. So there is demand for aluminum and particularly for primary aluminum to an extent that we do not have here.

The market is not a natural one. The aluminum is supplied to the Ministry of Supply, which is very capably staffed, the Ministry of Supply cooperates with an aluminum industry committee. They decide where aluminum can be used to advantage. Our principal comment a year ago was in regard to their lack of secondary alloys, and that was also brought out in the alloys the author mentioned. During the war there was a tremendous quantity of aluminum used in aircraft. In the days immediately following the war when the housing shortage was serious a large portion of aircraft scrap was re-rolled into sheets and used in an emergency housing program. However, there was still a considerable amount of aluminum scrap there which would have been available for castings except for the fact that there were no economies involved in using it. Industry paid the Ministry of Supply little more for 991/2 per cent aluminum than for the secondary aluminum alloys based on scrap.

As an example the use of primary aluminum there was 1300 metric tons of aluminum annually in pistons. In checking into the alloys involved, we found the bulk of that was of a highsilicon composition similar to our A132. They were apparently unfamiliar with the 66 or 75 alloys which are used for automotive pistons here, and, they used what, to us, is the more expensive and more difficult alloy. So behind all of these uses of aluminum there is government pressure and an economic reason for doing many things which we would probably not do here where we have a free choice of materials. If that fact is kept in mind in reading the paper, many of the uses brought out in the paper will seem more logical.

MR. HUDSON: I wish to point out that in our latest British Standard Specification 1490/1949, covering "Aluminum and

Aluminum Alloy Ingots and Castings," special provision is made for use of secondary metal made from accumulated war scrap.

Co-Chairman Brown: Do the aluminum-silicon alloys you use for sand, permanent mold, and die casting contain 11 to 13 per

Mr. Hupson: Such alloys are widely used in Great Britain especially for permanent mold and pressure die castings in view of their excellent fluidity

R. T. PARKER: 2 I had the fortune to be associated with the work which Mr. Hudson discussed and the work carried out by the British Non-Ferrous Metals Research Association. One thing that has emerged from this study is that grain size of itself is not important in the finished casting. Mr. Hudson has already said grain size in the solidified casting has certainly some effects on feeding. However, if you have finished castings with dissimilar grain size, it is not the grain size itself that has the effect on the properties. It is the alteration in distribution and size of unsoundness that you get in the casting.

I myself have had a good example of that in handling 220 alloy (Al, 10Mg). When you cast that alloy under normal conditions, there is, of course, unsoundness which slightly decreases the properties, and for the test bar with the large head cast vertically in good quality metal and cast under normal conditions, you probably will finish with 48,000 psi tensile and perhaps 10 per cent elongation.

On one occasion, we wanted to find out something about the related effect of soundness, and in doing so, we cast some of this metal on 100 psi air pressure. We found we could increase the properties to 50,000 psi tensile and 35 per cent elongation. As the authors of the paper pointed out, this is merely a demonstration of the effectiveness of soundness of casting and finally the distribution of unsoundness. In the present state of our knowledge we cannot hope to get large castings with perfect soundness so we have to hedge by getting normal castings, i.e., castings with a certain amount of unsoundness, and distribute that in the best possible way.

Co-CHAIRMAN BROWN: Have you found any connection grain size and the tendency for the casting to crack somewhere between solidification and the fusion point?

DR. PARKER: The tendency for cracking is less with the finer grain size in the alloy.

E. C. MANTLE: 8 There is one thing I would like to add to the remarks which have already been made about the British Non-Ferrous Metals Research Association's work on the theory of grain refinement. I think it is of some practical importance. In this work it has been shown that grain refinement is largely brought about by the carbides of grain refining elements of which titanium is the most common. These carbides are insoluble in aluminum at ordinary melting temperatures.

The important point about this theory is that grain refinement can only occur provided these carbides are present in the melt as small particles on which the molten alloy can crystallize during cooling. Unfortunately these carbide particles tend to dissolve in the molten aluminum as the temperature of the melt is raised and they are not readily reformed on cooling. So if the alloy is superheated to too high a temperature, say above 700 C (1292 F), these carbide particles are dissolved and are not present to form nuclei for crystallization during the freezing of the alloy in the mold. Consequently a coarse grained casting results. That is one reason why the melting temperature should be kept as low as possible. Usually the carbide particles are only reformed if the metal is solidified and remelted.

There is one other point to add to what Dr. Parker has said. We have some very striking photomicrographs showing the differences in the form of the porosity in fine grained and coarse grained castings. With a large grain size, the porosity occurs in the form of narrow cavities which stretch along the grain boundaries causing a very marked discontinuity; but with a fine grain size the cavities are small, isolated and fairly rounded in form and therefore much less harmful.

MEMBER: Where does the carbide come from to help grain

Dr. PARKER: The carbide is in the original material and comes from carbon electrodes in the production of aluminum. It is a very small amount but very little is necessary to effect grain refinement.

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8 Research, Non-Ferrous Metals Research Assn., London

NONFERROUS INVESTMENT CASTING

By

Hiram Brown*

THE TERM "INVESTMENT CASTING" is used by the author in preference to the usual term "precision casting" since the latter title is felt to be misleading. The word "precision" implies very close tolerances such as can be given by machining. Casting accuracy does not approach that degree of precision. In some cases, unfortunately, the inaccuracies of the investment casting process have resulted in its being facetiously labeled "the approximate casting process." Much of this ill feeling resulted from the industry overselling its product during the war. The investment casters could not produce the extremely close tolerances they advertised. Also, they accepted orders for parts which could have been made cheaper and better by some other process.

This all points to the fact that investment casting has definite limitations. So long as it is kept in its channels, it performs a useful purpose. With ferrous and high temperature alloys, many parts are precision cast because machining of the metal used is difficult or costly. In the nonferrous field this problem does not exist. The principal use of investment castings is, thus, for one of the following reasons: (1) parts are very small and can be cast many at a time in investment molds, (2) shape is such that it would be impossible to cast by other casting methods and too costly for other types of fabrication, (3) tolerances specified are closer than can be maintained by any other casting process, (4) tolerances or finish requirements are such that no machining or trimming of any sort is permissible over large areas and where parting line of any sort would not be permissible.

Costs of investment castings are high, so from the competitive, commercial picture, it seems safe to say that if the part can be made by any other casting process, investment casting should not be considered (except for small runs, as for experimental parts, etc.). During the recent war, cost meant little compared with

the necessity for obtaining parts. After the war, industry had time to redesign components in order to eliminate costly parts. This resulted in a tremendous decline in orders for investment castings and caused numerous investment foundries to shut their doors.

Early Difficulties

Part of the difficulties encountered by investment casters were due to the fact that the process mushroomed rapidly from a dental and jewelry business to a large scale, commercial process. The commercial parts were larger, more intricate, and more demanding in dimensions. The jewelers and dentists had the purpose not of precision in regards to measurements, but rather the accuracy desired was that of fidelity of reproduction. The industry had not had sufficient time for pilot or experimental work to determine the fundamentals necessary for large scale operation. Troubles were fought by hit and miss methods which amounted to guesses more than fundamental knowledge of the process. When price again became a factor, the high rejection rates made costs excessive, and due to lack of pilot or research work, many companies were unable to continue.

The result of this crisis was that the industry analyzed itself and tried a new approach. Advertising statements are much more subdued, and practical tolerances have been changed. This is well expressed by a statement from one of the leaders in the field. "Tolerances of 0.002 to 0.003 in. can be produced in small parts under closely controlled laboratory conditions. However, it now appears that commercial tolerance of \pm 0.005 in. per inch with a minimum of \pm 0.003 in. can be maintained in a majority of parts supplied in production quantities. When closer tolerances are required, a finish operation is generally necessary. Perhaps the fullest advantage of investment casting is gained by casting profiles to size and finishing bearing surfaces or other close tolerance surfaces by grinding or machining."1

Investment casting has been well defined in the following words: "An investment casting should be defined as a metal casting process employing an expendable pattern in conjunction with an unparted or one-piece mold. The mold is made of ceramic material di

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^{*} Chief Metallurgist, Solar Aircraft Company, Des Moines, Iowa.

Twenty-ninth annual official exchange paper from A.F.S. to I.B.F. presented at the 47th Annual Meeting of the Institute of British Foundrymen at Buxton, England, June 6 to 9, 1950.

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which completely covers the pattern and has no line of separation; i.e., no cope and drag."²

The practices for investment casting do not closely resemble those of any other casting process. It is similar to permanent molding in two respects: (1) the greatest dimension of investment castings is usually in the vertical position in the mold instead of horizontal as in the case of sand castings, and (2) the permeability of the mold is low and venting is a problem. It differs from permanent molding and die casting in the rate of cooling in the mold. It differs from permanent mold, die, and sand casting in that the casting cavities are almost completely hidden and the mold cannot be parted to examine it before pouring.

In general investment casting is a complex process that can be carried out only by thorough and continuous research into background principles and a careful check on the process as it is put into production. The close dimensional tolerances actually leave no margin for error. Because of the variety of factors involved, only general rules may be developed. Actually, each type of casting to be produced is a rule unto itself. A careful compilation of data and experience will allow a certain number of predictions to be made on the job at hand, but for the complete accuracy needed, only a careful study by the best "pilot shop" methods will insure a successful and economical production schedule. This means the necessity of a well equipped laboratory and sufficient financial stability to absorb experimental costs.

Pattern Materials

A variety of materials can be used so long as the patterns are expendable. Wax, plastic, frozen mercury, low melting point metals, and sulphur have all been tried. Wax, plastic, and mercury are the ones commonly used in production. The properties of each are very different and must be handled in a different manner.

Wax Patterns

Use of wax as pattern material is quite old and has been written about many times; therefore, only a summary of this subject will be given so that a comparison can be made with other pattern materials.

A wide variety of waxes are used, including beeswax, carnauba, and several crystalline type petroleum waxes. Some are proprietary and are sold in compounded form ready to melt. Some prefer to melt and mix their own waxes. The various ingredients all have special effects on the final product; for example, carnauba wax (vegetable base, hard, and has high melting point), albacar wax (synthetic wax, hard, and has high melting point), burgundy pitch and turpentine (both tend to lower the melting point and to impart some elasticity to the mix which would otherwise be very brittle). Not only must the ingredients be added in strict observance of a formula, but they must also be added in proper order since both of these factors influence the properties of the wax.

There are today a number of proprietary waxes in compounded form which give good results. It is probably better to select one of these than to try to blend a number of ingredients. One thing, however, must be observed closely. All waxes have different shrinkage characteristics, and once a wax is decided upon and pattern mold made, it may be impractical to change wax type without either retooling the molds or building new ones. This means that a great deal of experiment and thought should be carried on before finally selecting the wax which will be used in production.

Wax patterns are fragile and must be handled carefully to avoid chipping corners and edges. Even fingernails will cause deep scratches on wax patterns. Often it is necessary to place completed wax patterns individually with great care. Also, if allowed to sit around too long, wax patterns may become brittle and shatter like glass. On the other hand, small nicks, holes, or similar defects can be patched with a hot needle or knife blade or small soldering iron.

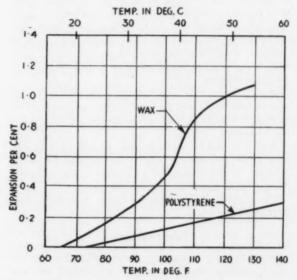


Fig. 1-Comparative expansion curves of typical wax plastic materials.

Figure 1 shows comparative expansion curves of typical wax and plastic materials. Wax in solid form has approximately four times the expansion that plastic has at the same temperature. This means that variations in temperature will cause much more dimensional change in the wax than in the plastic patterns. This points out the need for temperature control in sections where wax patterns are made, stored, or assembled for casting, since a noticeable change in room temperature may cause dimensions to be outside of tolerances before the patterns ever reach the mold.

Waxes have low melting points, about 170 F (77 C), and when melted are very fluid, flow readily, and take accurate reproduction of mold detail. However, they have high volumetric shrinkage in cooling from liquid to solid state and are subject to shrinkage areas unless properly and uniformly handled. This means controlling mold temperature, wax temperature (\pm 5 F or \pm 2.8 C) and injection pressure (whether hand or

automatic). Pattern molds must be well lubricated to

facilitate removal of wax patterns.

Wax which flows out of the mold can be reclaimed and re-used for certain purposes, such as for sprues, gates, etc., by simply remelting. It is not generally used for patterns, however, due to the fact that particles of ceramic or other foreign matter may cause bumps in the pattern, or if they fall out, will leave pits.

Due to its low melting point, wax can be easily injected into pattern molds. In many cases of short runs, the injection may be done by hand using a bulb syringe to furnish the pressure. However, it is difficult to hold close dimensional tolerances, not only from casting to casting, but from pattern to pattern, due to shrinkage characteristics of the wax and the non-

uniformity of hand injection pressure.

It is much better to use injection machines where pressure can be maintained constant. Simple machines of this sort can be bought for as low as about \$200. Wax injection requires less pressure than plastic injection, usually about 400 psi being adequate; therefore, the wax injection machines are much simpler and cheaper. Small wax extrusion machines can be purchased for making sprue stock of a wide variety of sizes and shapes.

Wax patterns can be set up or assembled by using hot needles or knives, or small soldering irons. Flat or round lengths of wax can be melted as filler mate-

rial if needed.

Wax can also be used on short as well as long run jobs since soft metal pattern molds can be made cheaply. Molds or dies for wax patterns can be made cheaper than those for plastic injection.

The cost of waxes is usually about three times that

of polystyrene plastics.

Plastic Patterns

Recently, plastic patterns have replaced waxes for many applications. Some of the advantages and disadvantages of plastics are discussed below. The usual plastic used for this purpose is polystyrene, so that material will be discussed first.

Cost is about one-third that of waxes. Polystyrene is plentiful and, since it does not require blending or mixing, properties are uniform. The patterns are harder and will withstand more handling and rougher usage than will wax patterns.

The specific gravity of polystyrene is 1.07 and shrink-

age 0.002 to 0.008 in. per inch of injection.

As shown by Figure 1, the expansion of polystyrene is considerably less than that of wax, thus making it less sensitive to temperature variations. The softening point of polystyrene is about 200 F (93 C) and it burns out at 750 F (400 C). It starts to distort under heat at about 150 F (66 C), and at about 180 F (82 C) there is a critical point where the plastic expands very rapidly. At 400 F (204 C) polystyrene is just soft enough to flow of its own weight, and at 600 F (316 C) will run rather freely; however, it never becomes a true liquid. It is isotropic (coefficient of expansion is independent of direction of measurement).

Polystyrene patterns can be held to very close toler-

ances by use of high pressure injection and do not tend to have some of the defects that waxes have, such as flow lines, misruns, etc.

Polystyrene, if marred, cannot be patched as can wax. Joining of plastic patterns is more difficult than joining wax patterns. Although plastic patterns can be welded together by use of a small soldering iron, this does not give a smooth joint. Best results are usually obtained by using plastic cement or solvents. Plastic cement can be made by dissolving some of the polystyrene in a suitable solvent, such as benzene. This method of joining is slow, since it takes some time for the cement to set enough to hold any weight, and joints tend to be rough. The best method makes use of a suitable solvent, such as benzene or carbon tetrachloride, using either a felt pad or blotter to absorb the solvent, against which the plastic parts are each touched before placing them together. The solvent may, also, be painted on with a brush. A few seconds are allowed for the solvent to penetrate the plastic, and then the edges are forced together with moderate pressure and held for about 5 to 10 seconds. To save time, small clamping devices could be made for this, thus freeing the worker's hand for assembling. The joined patterns may then be laid down upon a prepared surface so that there is no strain on the joint. Approximately one hour should be allowed before taking this pattern set-up to the next step.

Polystyrene is best in longer production runs, and wax is best for short runs. For that reason many investment foundries use both of these pattern materials. Steel dies are used for polystyrene patterns made in large quantities. Cost of setting up for injection of polystyrene patterns is expensive both from cost of machines (which may run up to \$20,000) and from the cost of sinking the steel dies necessary to maintain the dimensional accuracy required. The minimum run for polystyrene parts is about 5,000 pieces. For short runs bronze injection dies have been used, but die life is shorter. Use of hard steel dies makes it impossible to produce a master mold direct from the part to be produced as is sometimes done with soft metal master molds. Dies must be designed and sunk using blueprints of the part to be manufactured as a starting point. Since most plastic mold shops are not familiar with the dimensional accuracy required for such molds, it should be pointed out to them when the order is placed that the runners are almost as important as the casting cavities. This is due to the fact that the patterns must assemble correctly in setup. On simpler parts with liberal tolerances, it is often possible to machine a master from polystyrene bar stock and then investment cast steel cavities.

Polysterene Patternmaking

In polystyrene patternmaking, the pulverized polystyrene is loaded into a hopper from whence it feeds by gravity into the feed cylinder. As the injection stroke starts, a hydraulically actuated plunger forces fresh material into the heating cylinder where it is melted, and the melted material is pushed through an orifice into the tightly locked two-section mold cavity to form the piece. The time cycle can be set to work

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automatically and pressure is held as long as necessary to cool or harden the material in the mold, the plunger then moving back to allow new raw material to drop into the feed cylinder. With the pressure relieved, the mold is unlocked and a movable section moves away from a fixed section. The sprue disconnects from the melted material, taking hardened material in the orifice and leaving soft material at the point at which it pulls away. Ejector pins push the molded piece completely free from the mold. To maintain dimensional accuracy in the patterns, die temperature must be evenly held. Some injection machines also have a compression ram that works through the bottom platen. This is of particular advantage in the case of large patterns with one or more heavy sections. The piece can be injection molded and then given additional pressure by this ram to help overcome the inherent shrinkage in heavy sections.

Heating and cooling coils should be in the die to heat thin sections or cool heavy sections to equalize cooling and to help fluidity. If the mold becomes too hot, shrink areas may develop in the patterns and the overall cycle will become too long. If the mold is too cold, it will be difficult to fill cavities. Dimensional control necessitates strict mold temperature control. The chief problem in pattern production is to set up the proper cycle on the machine and then have sufficient controls to maintain uniformity of pattern.

It was found that on succeeding production runs it was possible to go into immediate and satisfactory production of the plastic machine by resetting the control on the machine according to the operation sheet set up during the first run. From this experience it is apparent that once the correct operational cycle is set up for a given mold, that mold can be returned to the machine for production at any time. This factor is of great value in the production of patterns for investment casting because it assures reproducible accuracy from one run to the next and eliminates the trial and error method of resetting the machine each time a mold is replaced.

In polystyrene injection, experience showed that 80 to 100 shots per hour could be produced in production with injection pressures up to 12,000 psi. After a few minutes warmup, the rejections by the operator for non-fills and other machine faults seldom ran over 2 per cent.

Cooling of Patterns

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Patterns are still slightly warm when removed from the mold and, consequently, must be laid out in such a manner as to prevent warping or distortion. If heavy sections of patterns join light sections, quenching in cold water after removing from machine tends to prevent shrinkage and distortion. The quenching of the patterns must be controlled closely or additional dimensional variations can be introduced. The chief factor to watch is that the patterns themselves are held in the die long enough to be set before putting them in water. If the pattern is not set first, the temperature of the water, length of time from mold to water, and any other of the human factors will cause variations in the mold to pattern shrinkage ratio. If this happens

then part of the efficiency of the plastic pattern is lost. The patterns should remain in the water bath for approximately five minutes. The cold water sets the outside of the plastic and prevents flow or warpage. After the patterns are cool, they may be stacked in pans or other containers. However, if storage is for a considerable period, then care must be taken to prevent warpage, because the plastic will distort if placed under even small stresses for a long period of time. Another precaution to observe is to handle the patterns in such a manner as not to scratch the surface. Polystyrene can be scratched by fingernails, metal corners, or even the sharp corner of another pattern. This causes no trouble if reasonable care is taken in handling the patterns.

Polystyrene can be cut if it is not allowed to become hot; otherwise, the plastic will gum up, and there is also the chance of warping the pattern. Ordinary finishing equipment may be used if the proper adjustments as to speed, use of coolants, etc., are made.

Polystyrene sprues, rejected patterns, flask, etc., can be reclaimed for use. The machines for this purpose are more literally choppers than grinders. Polystyrene cannot be ground because the heat formed will gum up the material in a normal grinder. Adding dry ice during chopping or grinding helps to keep the temperature low. Only 20 to 30 per cent of used and reground material should be added to new material.

Granular polystyrene is used and should be clear and uncolored, or colored by dyes rather than pigments, since evidence indicates that pigmented colors tend to leave residues behind in the ceramic mold that are difficult to remove in burnout. Plastic patterns with some color are much easier to inspect for small defects than are clear, transparent ones. The plastic should be dried at low temperature before loading in the molding machine; otherwise, the surface moisture will form steam bubbles in the pattern. Some of the bubbles may be near enough to the surface to cause rejection if the metal breaks through this bubble during pouring.

Plastic Molds

For some parts where quantity is low and shape is not too intricate, the patterns can be machined for solid plastic, set up, and metal cast in the resulting cavity. Figure 2 shows examples of simple patterns

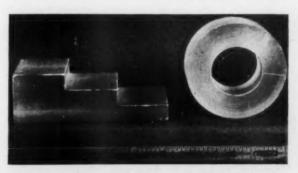


Fig. 2-Simple patterns machined from solid polystyrene plastic.

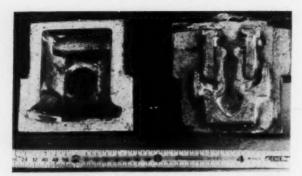


Fig. 3-Part cast from machined plastic pattern.



Fig. 4—Residual stresses in a plastic pattern after machining.

machined from solid plastic. Figure 3 shows a part cast from machined patterns. In this connection one fact should be pointed out. Patterns machined from plastic should be used without too much time elapsing after machining or the parts may crack due to residual stresses. That plastic will retain residual stresses after machining is shown in Fig. 4. Photographs were made using polaroid projector and monochromatic light source, (5461 Angstrom green line of a mercury arc). It is possible that low temperature anneal of the plastic patterns would relieve these stresses. It is of interest to note that irregular, high internal stresses in plastic patterns such as shown in Fig. 4 will alter the coefficient of linear expansion.

Recently a new type of plastic for patterns has made its appearance. It is made by using the lower molecular weight polystyrenes. It does not have a critical point above room temperature so that the expansion characteristic above room temperature is a straight line. Thus, no mold cracking will result from a sudden expansion. Recommended pressure is about 1,000 psi at 350 F. For this reason neither expensive injection molding machines nor steel dies are necessary. Zinc alloy dies can be used. The material can be molded on practically any wax machine as long as a pressure of somewhere around 1,000 psi is available. However, to use it in most wax injection machines would require some changes in pressure application. Wax melts and is injected like die casting with the

pressure piston coming in at right angles to the flow of wax from the holding pot. Plastic does not become truly liquid, so pressure must be exerted from top of holding pot to force the flow of plastic. There is also evidence indicating that it molds with much less residual stress than normal polystyrene. Cost is about thrice that of polystyrene. Shrinkage is about that of wax with a spread of 0.6 to 1.2 per cent. Above 308 F (150 C), it will flow freely out of the mold and is almost completely removed from mold at 800 F (427 C).

Investment Material

In general an investment material contains two major constituents: a refractory and a binder. When mixed with water or other suitable vehicle, the satisfactory investment will become sufficiently plastic to be formed easily into a mold. It must have sufficient working time to allow for convenient handling, and then set into a hardened mass with reasonable speed. The investment must not contain impurities which would be injurious to the metal being cast, and it must not be fused by the molten metal. It must not crack during burnout, or in cooling to room temperature after burnout, and it must withstand heat shock of molten metal being poured into it. It must be hard enough not to erode during pouring, yet should break up easily after the metal has solidified so that the casting can be readily removed. In general, 2000 F (1095 C) is the dividing line between high and low temperature investments, since gypsum starts breaking down above 2050 F (1120 C).4

Fortunately, most nonferrous work falls into the low temperature class. This type of investment usually contains one of the three forms of silica (to be discussed later) with gypsum binder. A satisfactory combination contains about 70 per cent 200-mesh cristobalite silica and 30 per cent gypsum. To this mixture is added about 33 per cent water. Usually, proprietary, premixed investments of this sort are readily available, and it is only necessary to add water. If the silica and gypsum are bought separately and mixed in the foundry, extreme care must be used to assure that uniform grain size and distribution is maintained, since this will greatly influence the performance of the wet mixture.

A slightly stronger ceramic is needed for polystyrene patterns than for wax. This can be obtained by using the same ceramic mixture but reducing the amount of water added.

Silica can exist in at least six crystalline forms: both high and low temperature modifications of quartz, tridymite, and cristobalite. These various forms of silica differ only in atomic arrangement. In changing from low to high temperature modification, re-arrangement of atoms occurs and expansion takes place. The amount and rate of expansion differs with the type of silica as shown in Fig. 5. Cristobalite undergoes a rapid expansion at slightly under 500 F (260 C). Quartz undergoes a rapid expansion at about 1050 F (566 C). Tridymite undergoes two small expansion increases at about 250 F (120 C) and 325 F (163 C), but the change is much less than that of quartz or cristobalite. Fused quartz undergoes the least expansion,

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Fig. 5—Amount and rate of expansion of various forms of silica. Note that most of the quartz expansion occurs near its inversion temperature (approx. 573 C, 1063 F).

less than 0.1 per cent up to 1600 F (870 C). The volume of all forms of silica is comparatively constant above about 1110 F (600 C), so if silica were heated and cooled, but always remained above this temperature, there would be little cracking or spalling. However, when the material is cool and heat is applied, cracking is likely to occur at the point where the change from low to high temperature modification occurs, and, similarly, when cooling from above 1110 F (600 C) to a temperature below the modification change point.

Figure 6 shows a typical heating and cooling curve of a mixture of cristobalite and gypsum.

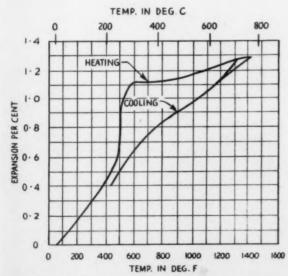


Fig. 6-Typical heating and cooling curve of a cristobalite investment for nonferrous metals.

Preparation of Investments

In any investment it is important to control particle size. Finer powders are too dry with the same water content as coarser ones, because of the increased surface area which must be wet to obtain the same degree of workability. Particle size can be controlled by elutriation and hydrometer checks. The values are expressed as median particle size, which is determined by hydrometer measurement during sedimentation of water suspension. The casa grande hydrometer method⁵ is used since the finest screen made retains only those particles larger than 37 microns (0.001443 in).

Moisture control is also important since increasing moisture decreases strength, and increases shrinkage, drying time, and cost. A Braebender moisture tester is excellent for this purpose.

In mixing add dry material to liquid. This mini-

mizes sticking to sides of container.

After investment is mixed, it is usually necessary to vacuum it to remove bubbles of air. A vacuum of 29 to 29.4 in. of mercury is necessary to complete removal of bubbles. After being held 3 min under vacuum, it is vibrated on vibratory table. When pouring the ceramic around the patterns, the flask is vibrated. The mix should be poured down the side of the flask but not directly on the patterns. The flask may be rotated during this operation to assist in removing bubbles. The flask is filled to a point just covering patterns and is then vacuumed. The flask is then completely filled and allowed to stand for one or two minutes on vibrator. After about an hour, it is hard and the base can be removed. Do not vibrate too long or some settling action will occur in the flask. This would cause areas of varying strength and, subsequently, more subject to cracking. Also, sufficient vacuum should be obtained and held to pull the air out of the mixture and to free any trapped air bubbles in recessed portions of the patterns.

Drying, Elimination of Patterns, and Burnout

After the investment is in the flask, there is a threestage operation that is necessary to prepare the mold for pouring.

- 1. Drying out moisture.
- 2. Eliminating patterns.
- 3. Burnout, or baking of ceramic.

Where wax patterns are used, steps 1 and 2 coincide. However, when polystyrene patterns are used, the first step only drives off excess moisture. The plastic patterns burn out or eliminate at a temperature between that used for drying and that used for baking the ceramic. All molds are placed in drying oven bottom side up so that the pattern material can drain out as it melts. Also, the wax holding the flask onto the base melts and the base can be removed and used again.

With wax patterns a dryout temperature of 200 to 220 F (93 to 104 C) for several days effectively dries the mold and eliminates the bulk of the pattern wax. When polystyrene patterns were used, it was found that this drying temperature had a great effect on the cracking of the molds. Castings made with polystyrene patterns developed fins due to cracked ceramic. It ap-

peared to be connected with the expansion of the patterns during the time the ceramic had not attained strength. It has previously been mentioned that polystyrene has a critical point at about 180 F (82 C) where the plastic expands rapidly. At this time the ceramic is weak and the expansion of the patterns causes cracks to open up. By reducing the dryout temperature to 140 to 150 F (60 to 66 C), the tendency for these expansion cracks was greatly lessened.

When wax patterns were used, it was customary to load the burnout oven and increase the temperature 100 F (55 C) per hour until 1200 F (650 C) was reached. The molds were then held at 1200 F (650 C) for 12 hr to thoroughly eliminate any wax or carbon residue. This length of time was found to be desirable if minute surface pits due to reaction of residue with metal were to be avoided. The long period was made necessary by the fact that the wax soaked ½ in. or more into the investment during the dryout and had to be drawn out by the use of extensive heat, due to the low permeability of the ceramic. This made an overall burnout cycle of 24 hr.

Precautions In Drying

When the wax dryout and burnout technics were used for plastic patterns, rough casting surface often appeared in small areas of castings. The appearance was visible as a dull finish compared with the shiny, smooth finish of the normal casting. This defect was never deep but gave bad appearance. This condition appeared only when plastic patterns were used. It disappeared when drying temperature was lowered to 140 to 150 F (60 to 66 C) and burnout cycle was speeded up. Instead of the usual 100 F (55 C) per hour, the rate of heating was increased to 160 F (70 C) per hour. This decreased the heating cycle from 12 to 71/2 hr, a reduction of 37.5 per cent. It is believed that the condition resulted from the heavy viscosity of the plastic. Wax, when it melts, runs freely and flows from the mold like water, but plastic never becomes a true liquid. It goes through a syrupy stage and flows only slowly. It appeared that this syrupy liquid actually would lie in certain areas of the mold and infiltrate slightly into the investment. Then as temperature rose still more, and the material flowed out, it took very small particles of investment with it. By increasing the heat input, the plastic apparently did not have time to lie long enough in the syrupy state to soak into the investment. So far as the experiments went, the more rapidly the plastic was removed, the better the finish of the castings.

It was further found that soaking time at 1200 F (650 C) could be reduced from 12 hr to 2 hr when plastic patterns were used. This is a reduction of 10 hr or 83 per cent in soaking time, and a reduction of 14½ hr or 60 per cent in overall burnout cycle. The saving in soaking time is due to the fact that due to its greater surface tension, plastic does not soak into the investment to the extent that wax does. It was not practical to reduce the soaking time below 2 hr since if all carbon residue was not removed, it would produce areas having a rough casting surface.

Where gypsum is used in the investment, the burn-

out temperature should not exceed 1200 F (650 C) or the calcium sulphate will break down. Temperature must be at least 1000 F (540 C) to effectively remove carbon. In any event, the rise in temperature must be delayed at about 1100 F (593 C) inversion point of silica; otherwise, mold cracking can be expected due to rapid expansion at this temperature.

If moisture is not removed and the mold is fired too hard, the formation of steam in the ceramic may crack it or cause a spalling on the interior faces of the

cavity.

All of this indicates the necessity for good temperature control on all furnaces, including controls which allow rapid and constant increase in temperature as well as uniform holding temperature. Arrangement must also be made to introduce sufficient oxygen into the furnace to burn the large volume of plastic readily. If combustion of the plastic is not fast enough, very heavy deposits of carbon will form that are difficult to burn out and will materially affect overall burnout cycle.

When wax patterns were used, pits occurred on tastings as a result of small parts of investment dropping into the mold prior to or during casting. These were severe enough to cause high rejection rates. Careful burnout and handling did not seem to correct more than a minor part of this trouble. However, this type of defect decreased considerably when plastic patterns were used. It is believed that the principal cause of this condition was the fact that when the wax melted it soaked into the investment during the drying cycle. When the molds were fired the wax tried to force its way out of the investment, and quite often the evolution was violent enough to lift small flakes of investment to form a scabbing condition. When metal was poured into the flask, it either broke loose those films of investment or flowed around them, in either case resulting in pits.

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With nonferrous alloys it has been found that to obtain best results, the molds must be cooled before pouring. Different alloys require different mold temperatures, so the range of mold temperature may be from 1000 F (538 C) to room temperature. To save time, to give more flexible operation, and increase output, it is much better to use small, batch-type furnaces for low temperature work than to use one big, continuous burnout furnace. The cooling rate will be faster, and the molds requiring low pouring temperatures can be segregated from those requiring higher pouring temperatures, so that it is not necessary to wait for a large furnace to cool through the respective ranges before finally casting the molds requiring low pouring temperature.

Gating Practice or Set Up of Patterns

Set up of patterns is their joining or assembly into a unit which will be invested and cast. It is evident that if set up is to be economic and efficient, then much forethought must go into the original design of the dies or molds. The first object is to get into the mold as many pieces as can be cast at one time with satisfactory quality. The second consideration is how this can be done rapidly in production. If the number of

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parts required is small, or if the patterns are to be made singly of wax, then not so much advance thought is concerned since by hand work the parts can be set up in almost any conceivable manner. However, if the patterns are to be made in groups, or still more, if injection dies are to be made for large production runs, then planning must start with mold and die design. This is so because if assemblies are to fit together right, the patterns must be produced in size and shape that will readily lend themselves to rapid assembly. Wherever possible on injection made patterns, the gates and runners which admit plastic to the die cavities should be of such design that they may also be used to introduce metal into the mold cavities after the patterns have been eliminated. If these are properly designed, then for assembly it is only necessary to remove the sprue from the pattern and assemble the patterns complete with gates and runners. Then the pouring sprue necessary for the entire assembly is attached. For this purpose wax or plastic bars, flats, or other standard shapes can be used.

Small pits can result if patterns are designed so that they do not join solidly when placed together and tiny spaces are left. Unless those spaces are filled with plastic or wax during set up, the investment will flow into those cracks and result in fins of investment which will wash into the mold.

Arrangement of Patterns in Investment

In arranging set up, the following factors should be considered: ease of assembly and cementing, drainage from the ceramic mold, flow of metal into the cavity, and cut-off operation. Either round or rectangular flasks may be used. At least 1/2 in. should be allowed for ceramic wall between patterns and flask wall, and at least 3/4 in., preferably 1 in., from bottom of set up to bottom of flask. These thicknesses are necessary to insure sufficient strength for the ceramic to withstand expansion of plastic, thermal shock, and pressure used in casting the metal. Actual arrangement of patterns should be such that the minimum of straight lines or planes of weakness will be formed in the ceramic. Usually 1/8 to 1/4 in. should be allowed between patterns or rows of patterns. Staggered set ups help prevent cracks when using plastic patterns. That is, if the first row has heavy shoulder of the set-up on the outside, then the next row should be reversed so that a thin, plastic dimension is on the outside. Where this is impossible or impractical, then the rows should be offset by having every alternate row indented so that no two adjoining rows have a heavy section at the same level. It appears that stresses set up during expansion cause cracks which will tend to be continuous across the mold unless lines of stresses are broken up. The staggering of set-ups will break up these stress lines.

Sometimes cracking occurs around areas which have large sections of plastic, such as sprues and risers. This can be helped considerably by placing a wax pad around those areas of the pattern. This can be accomplished by actually placing small strips of wax at any areas where large plastic concentrations occur, or by dipping that part of the set-up in molten wax. The

wax cushion melts and flows out of the mold before the critical expansion temperature of the plastic is reached, thus leaving room for the plastic to expand without pushing against the investment.

If flasks are made to too thin metal, cracking of molds is much worse than if heavier gage material is used. It was found that stainless steel sheet of no less than 0.060 in. thickness should be used for flasks. Experiments were made using stainless steel flasks 0.037 and 0.060 in. thickness respectively. All of the castings in the lighter flasks exhibited more flash or fins than those in the heavier flasks.

Similarly, casting the same parts in both square and round flasks resulted in more flash or fins in those cast in square flasks than those cast in round flasks of the same gage.

Melting, Pouring, and Metallurgical Considerations

Melting—Melting can be done in the usual types of equipment, except that very small furnaces are used. Nonferrous alloys can be melted in induction furnaces, or in oil, or gas-fired stationary or tilting furnaces. Usually silicon carbide or clay-graphite crucibles are used due to their ability to retain heat while metal is being carried to molds, and because they do not tend to contaminate the metals with undesirable impurities. In any case, good temperature controls should be exercised.

The standard alumel-chromel or similar immersiontype thermocouples may be used. To assure accurate measurement, it is recommended that closed-out thermocouples be used. These are less likely to give erratic readings than are open-end types for most metals. Many portable pyrometers of this type, capable of measuring up to 2500 F (1370-C) are available and can be used either while the pot is in the furnace or anywhere in the shop where metal is being poured.

As in other foundries, metals are subject to gas pickup during melting and usual fluxing or degassing methods used in other types of foundries should be applied to the metal.

Pouring—There are four possible methods of pouring investment castings: (1) static pour, (2) centrifugal pour, (3) applied pressure, (4) vacuum. There have been reports of successful vacuum casting, but the author's experience has included only the first three methods.

If a part can be poured statically with good results, then there is no reason to pour it in any other manner. Usually, however, due to the small size and thinness of investment cast parts, it is necessary to apply additional force to assure complete filling of the mold. This force can be supplied by centrifugal action. It can also be obtained by pouring the metal statically and immediately lowering a pressure head over the mold and literally blowing the metal into the mold. Where stationary pressure heads are used, the molds are rapidly placed under the head, poured, and the pressure head tightened to seal against the top of the mold and air pressure turned on. This usually results in sprues which suck down and practically disappear when the pressure blows into the mold. (Figure 7 shows a pressure casting machine.)

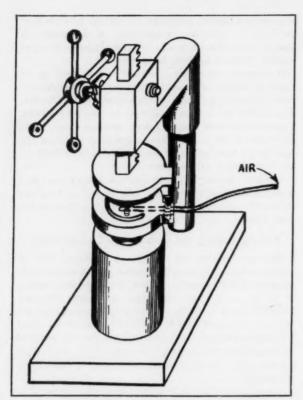


Fig. 7-Air-pressure casting machine for investment molds.

Centrifugal casting requires extremely good co-ordination if misruns are to be avoided, and in general is a more complicated process. Although this method of investment casting is used a great deal, the author in his own experience did not find any cases where centrifugal casting would successfully cast parts which could not be successfully cast by application of applied pressure.

In the case of applied pressure, there were two defects which were most prevalent: (1) non-fills, and (2) cold shuts or misruns. These in turn were due primarily to two factors: (1) insufficient pressure, and (2) cold metal. Non-fills are castings which have not been completely filled with metal. The cause seems to be lack of pressure. Insufficient pressure is the result of leakage. Pressure is usually maintained solely between the pressure head and the top of the mold. Thus, if a crack forms which extends to the edge of the mold, a pressure leak occurs and full pressure cannot be maintained. Pressure leaks develop along cracks in the ceramic, or at places where patches or ceramic drop off during burnout, or due to improper clamping of the pressure fixture. Some relief is possible if a pressure chamber is used to surround the mold. For example, a cylinder slightly larger in diameter than the mold can be sealed tightly to the pressure head of the pressure machine with the bottom left open. This can be lowered around the mold and sealed in rubber or some material which will make a tight seal. Then when pressure is turned on, the cylinder

becomes a pressure chamber and does not have to depend on the top of the ceramic as a seal. This is also helped by substituting quick action valves in place of hand valves so that pressure is on as soon as the valve is touched by the descending cylinder or some similar arrangement.

Misruns are defects where metal does not quite fill sharp corners or recesses or does not run the expected distance in the mold. Cold shuts are defects where streams of metal meet but do not merge to form a solid part. Both of these defects are usually due to cold metal. This can result from transportation of molten metal from furnace to mold, or from transfer of metal from furnace to cold ladle, or from improper temperature measurement. When induction furnaces, or stationary or tilting oil or gas-fired furnaces are used, the metal must be carried from the melting furnace to the pressure machines. Even though this is but a short distance, heat loss is high because of the small amount of metal melted each time. In the case of gas or oil-fired pots, the crucibles can be lifted out of the melting furnace and carried to the mold, thus minimizing heat loss since no transfer of metal is involved. However, where stationary induction furnaces are used, the metal must be transferred to a ladle and then carried to the mold, thus involving considerable heat loss. This condition can be overcome by using induction furnaces equipped with switch contacts in the floor under the furnace. The lifting of the furnace breaks the circuit at the switch, and since all power cables can be fastened to the floor switch and none left on the furnace to interfere with lifting, the entire melting portion can be lifted and carried to the molds with a minimum heat loss.

Metallurgical Considerations

There are a number of meuallurgical factors to be considered, such as: (1) gating, (2) mold temperature, and (3) metal temperature. Gating includes getting the metal into the mold cavity in a manner which will assure sound castings with good mechanical properties, and passages for the escape of air or mold gases, which, due to the tightness of the ceramic, cannot escape through the mold walls.

It is believed in many quarters that very thin gates and small risers can be used on investment castings which proportionately are much smaller than those used on sand castings. This quite often leads to serious trouble, particularly in regard to mechanical properties. The author has previously shown⁶ that with plaster castings, it is possible to increase tensile 13 per cent, and elongation 50 per cent, and reduce microshrinkage by increasing the size of gates and risers.

It is, furthermore, often stated that investment test bar properties are lower than sand cast values. This is not true if proper test specimens are used and proper gating employed. Since investment castings are smaller than sand casting, it is only fair that a test specimen smaller than 0.5 in. diameter be used. Therefore, the standard tensile specimen for investment casting in 0.25 in. in diameter and has 1-in. gage length. Usually $\frac{3}{8}$ in. threaded shoulders are cast on the specimen and the bar pulled as cast. (See Fig. 8.) Using this speci-

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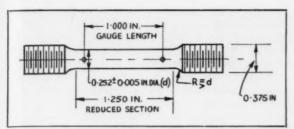


Fig. 8—Tensile-test specimen for investment-cast materials.

men, tensile properties equivalent to sand-cast properties can be obtained. Pouring temperatures can be kept lower than for sand casting, and fine grained, sound castings can be produced.

Nonferrous Gating

In order to obtain best results with these alloys, a wide range of mold and metal temperature is necessary. Two types of test bar gating were tried: (1) direct pour, and (2) indirect pour. These are shown in Fig. 9 and 10, respectively. In each case external pressure of 20 lb was applied immediately after pouring.

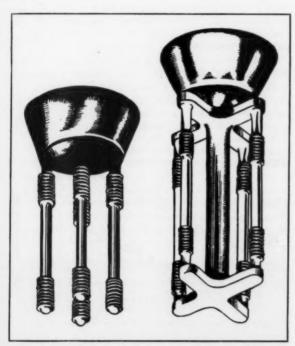


Fig. 9 (Left)—Direct test-bar gating system. Fig. 10 (Right)—Indirect test-bar gating system.

First, aluminum alloys were tried. Nominal compositions of the alloys used are shown in Table 1.

From the data in Table 2, it can be seen that all of the aluminum alloys tested gave best results with direct pouring.

Using the direct pour gating, further tests were made with two aluminum alloys to determine the effect of pouring temperature on tensile properties of test

TABLE 1-NOMINAL CHEMICAL COMPOSITION OF ALUMINUM ALLOYS USED

| Alloy | Copper | Silicon | Magnesium | Zinc | Chromium | Titanium |
|-------|--------|---------|-----------|------|----------|----------|
| 195 | 4.5 | 0.8 | _ | _ | - | _ |
| 355 | 1.3 | 5.0 | 0.5 | - | | _ |
| 40E | _ | _ | 0.5 | 5.5 | 0.5 | 0.2 |

Table 2-Tensile Properties vs Type of Gating of Test Bars

| Alloy | Condition | Gating | Tensile, I psi | Elongation % in 2 in |
|-------|--------------------|----------------------|-------------------|-------------------------|
| 195 | As-Cast | Direct Pour | 20,200 | 2.6 |
| 195 | As-Cast | Indirect Pour | 16,400 | 1.1 |
| 355 | As-Cast | Direct Pour | 26,400 | 2.0 |
| 355 | As-Cast | Indirect Pour | 24,760 | 2.0 |
| 40E | 3 days room temp. | Direct Pour | 31,300 | 10.0 |
| 40E | 3 days room temp. | Indirect Pour | 30,000 | 10.0 |
| 40E | 10 days room temp. | Direct Pour | 35,000 | 11.0 |
| 40E | 10 days room temp. | Indirect Pour | 33,400 | 10.0 |

TABLE 3—EFFECT OF METAL TEMPERATURE ON TENSILE PROPERTIES USING DIRECT POUR

| Alloy | | Metal Te | mperatur ° C | re Tensile, psi | Elongation % in 2 in. |
|-------|--------------------|------------|-----------------|--------------------|-----------------------|
| 195 | As-Cast | 1350 | 732 | 13,000* | 1.0* |
| 195 | As-Cast | 1325 | 718 | 22,500 | 2.5 |
| 195 | As-Cast | 1300 | 705 | 22,800 | 4.0 |
| 195 | As-Cast | 1275 | 690 | 22,700 | 3.5 |
| 195 | As-Cast | 1250 | 677 | 22,100 | 4.0 |
| 40E | 3 days room temp. | 1350 | 732 | 32,200 | 15.5 |
| 40E | 3 days room temp. | 1325 | 718 | 31,600 | 13.5 |
| 40E | 3 days room temp. | 1300 | 705 | 31,900 | 13.5 |
| 40E | 3 days room temp. | 1275 | 690 | 31,800 | 13.0 |
| 10E | 3 days room temp. | 1250 | 677 | 31,400 | 14.0 |
| 10E | 10 days room temp. | 1350 | 732 | 33,250 | 12.0 |
| 40E | 10 days room temp. | 1325 | 718 | 35,200 | 11.0 |
| 40E | 10 days room temp. | 1300 | 705 | 35,100 | 11.0 |
| 10E | 10 days room temp. | 1275 | 690 | 34,600 | 12.0 |
| 40E | 10 days room temp. | 1250 | 677 | 35,150 | 11.0 |
| | * Bars had o | coarse, de | ndritic st | ructure. | |

TABLE 4—EFFECT OF MOLD TEMPERATURE ON TENSILE PROPERTIES USING DIRECT POUR

| | Metal per | Elon- gation, % in | | | | | |
|------------------------|--------------|--------------------------|-------|-------|----------|-------|--|
| Metal | ° F | ° C | °F °C | | psi | 2 in. | |
| 195 As-Cast | 1300 | 705 | 400 | 205 | 19,400 | 4.0 | |
| 195 As-Cast | 1300 | 705 | Room | Temp | . 23,850 | 3.0 | |
| 40E 3 days room temp. | 1300 | 705 | 400 | 205 | 29,300 | 11.5 | |
| 40E 3 days room temp. | 1300 | 705 1 | Room | Temp. | . 29,850 | 12.0 | |
| 40E 10 days room temp. | 1300 | 705 | 400 | 205 | 34,000 | 11.5 | |
| 40E 10 days room temp. | 1300 | 705 1 | Room | Temp. | 33,600 | 10.0 | |

TABLE 5-NOMINAL CHEMICAL COMPOSITION OF COPPER-BASE ALLOYS USED

| Alloy | Zn | Sn | Pb | Si | Mn | Al | Be | Co | Fe |
|------------------|----|----|----|----|-----|-------|-----|-----|-----|
| Silicon Brass | 14 | _ | _ | 4 | _ | _ | - | - | - |
| 85-5-5-5 | 5 | 5 | 5 | _ | - | rame. | - | _ | _ |
| Beryllium Copper | - | - | | - | - | _ | 2.0 | 0.5 | _ |
| Manganese Bronze | 38 | - | - | - | 1.0 | 1.0 | | _ | 1.5 |

Table 6-Sand-Cast Specifications for the Copper-Base Alloys Shown in Table 5

| | | M | in. Elongation, |
|---|------------------|-------------------|-----------------|
| | Alloy | Min. Tensile, psi | % in 2 in. |
| - | Silicon Brass | 60,000 | 16 |
| | 85-5-5-5 | 30,000 | 20 |
| | Beryllium Copper | 80,000 | 20 |
| | Manganese Bronze | 65,000 | 20 |

TABLE 7-TENSILE PROPERTIES VS TYPE OF GATING OF TEST BARS

| Alloy | Gating | Tensile, psi | Elongation, % in 2 in. |
|------------------|----------|--------------|---------------------------|
| Silicon Brass | Direct | 72,000 | 30.7 |
| Silicon Brass | Indirect | 71,000 | 28.7 |
| 85-5-5-5 | Direct | 39,200 | 39.7 |
| 85-5-5-5 | Indirect | 40,000 | 38.2 |
| Beryllium Copper | Direct | 82,500 | 24.0 |
| Beryllium Copper | Indirect | 75,800 | 16.5 |

TABLE 8-EFFECT OF METAL TEMPERATURE ON TENSILE PROPERTIES USING DIRECT POUR

| Metal | Metal * | Femperatur ° C | e Tensile Strength, psi | Elongation % in 2 in. |
|------------------|---------|-------------------|----------------------------|-----------------------|
| Silicon Brass | 1750 | 955 | 64,700 | 29.0 |
| Silicon Brass | 1700 | 930 | 66,200 | 27.0 |
| Silicon Brass | 1650 | 900 | 65,700 | 29.0 |
| Silicon Brass | 1600 | 870 | 62,500* | 11.0* |
| 85-5-5-5 | 2050 | 1120 | 36,000 | 36.0 |
| 85-5-5-5 | 2000 | 1095 | 37,300 | 41.0 |
| 85-5-5-5 | 1950 | 1065 | 38,700 | 40.0 |
| 85-5-5-5 | 1900 | 1040 | 35,500* | 28.0* |
| Beryllium Copper | 2000 | 1095 | 78.750 | 26.5 |
| Beryllium Copper | 1950 | 1065 | 77.850 | 28.0 |
| Beryllium Copper | 1850 | 1010 | 81,000 | 29.0 |
| | * Dro | ss in Bars | | |

TABLE 9-EFFECT OF MOLD TEMPERATURE ON TENSILE PROPERTIES USING DIRECT POUR

| | W-100 | etal erature | - | lask erature | Tensile, | Elon- gation, |
|------------------|----------------|-----------------|------|-----------------|----------|------------------|
| Metal | ° F | ° C | ° F | ° C | psi % | in 2 in |
| Silicon Brass | 1700 | 930 | 900 | 480 | 70,870 | 28.25 |
| Silicon Brass | 1700 | 930 | Room | Temp. | 74,400 | 27.0 |
| 85-5-5-5 | 1950 | 1065 | 900 | 480 | 36,600 | 27.0 |
| 85-5-5-5 | 1950 | 1065 | Room | Temp. | 24,000* | 9.0 |
| Beryllium Copper | 1850 | 1010 | 1100 | 595 | 83,500 | 21.5 |
| Beryllium Copper | 1850 | 1010 | 900 | 480 | 84,500 | 24.0 |
| Beryllium Copper | 1850 * Bars | 1010 contai | | Temp. | 83,000 | 28.0 |

TABLE 10-EFFECT OF GATING ON TENSILE PROPERTIES OF MANGANESE BRONZE

| | | | etal erature | Tempe | | Tensile, | Elon- gation, | |
|----------|--|--------|-----------------|---------|-----|----------|------------------|--|
| Gating | | ° F | ° C | ° F | ° C | psi % | in 2 in | |
| Direct | | 1850 | 1010 | 900 | 480 | 71,500* | 16.4* | |
| Indirect | | 1850 | 1010 | 900 | 480 | 74,500 | 20.5 | |
| Direct | | 1850 | 1010 | 900 | 480 | 58.800* | 15.20 | |
| Indirect | | 1850 | 1010 | 900 | 480 | 63,790 | 26.5 | |
| | | * Bars | contai | ned dro | SS. | | | |

specimens. The two alloys showed very little sensitivity to pouring temperatures used as shown in Table 3. However, 1350 F (732 C) was too high for 195 alloy and gave very bad results. In general, 1300 F (705 C) appeared to be satisfactory for both.

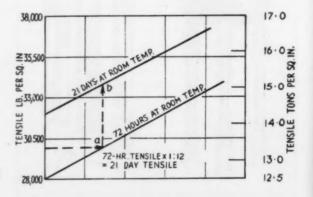
The same two aluminum alloys were used to determine the effect of mold temperature on tensile properties of test bars. The two alloys showed a different degree of sensitivity to mold temperatures as shown by Table 4. The aluminum-copper alloy showed definite increase in strength when mold temperature was reduced from 400 F (205 C) to room temperature. The aluminum-zinc-magnesium alloy showed very little change with variation in mold temperature.

Similar tests were conducted on copper-base alloys. The nominal chemical composition of the alloys used are shown in Table 5. Table 6 shows the sand cast requirements for the alloys tested.

The effect of gating on tensile properties is shown in Table 7. Again as in the case of aluminum alloys, the copper-base alloys gave best results using direct pour methods.

The effect of metal temperature is shown in Table 8. In each case there was an optimum pouring temperature which gave best combination of tensile and elongation. This was 1650 F (900 C) for silicon brass, 1950 F (1065 C) for 85-5-5-5, and 1850 F (1010 C) for beryllium copper. When pouring temperature was too low, dross remained in the bars, so pouring ranges must be selected to avoid this condition.

Table 9 shows the effect of mold temperature on tensile properties. The 85-5-5-5 alloy showed a tendency to trap tough oxide film when mold temperature was too low. The other alloys showed little variation in properties with decreasing mold temperature, and, in general, 900 F (480 C) mold temperature seemed to



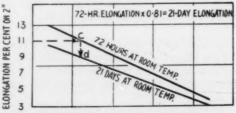


Fig. 11-Effect of aging on tensile strength and elongation of 40E aluminum alloy.

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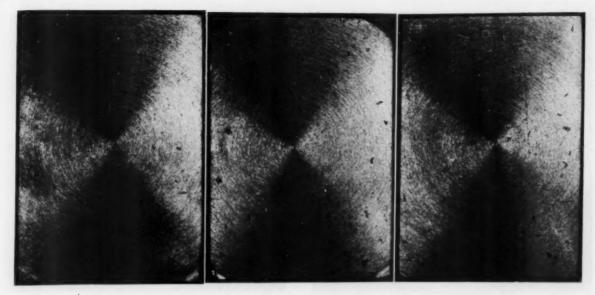


Fig 12—Pinhole formation at 760 C Fig. 13—Pinhole formation at 980 C Fig. 14—Pinhole formation in 355 (1400 F) in 355 aluminum alloy. (1800 F) in 355 aluminum alloy. (\times 2.19.) Fig. 14—Pinhole formation in 355 aluminum alloy aluminum alloy after cooling to 760 C (1400 F). (\times 2.19).

be satisfactory for all of these alloys.

Manganese bronze was the only alloy tested which could not be made to produce uniform and satisfactory results. If tensile was satisfactory, elongation was too low. If elongation was satisfactory, tensile was low. Table 10 shows typical examples of this. Numerous specimens were cast but the answer to the erratic results was never found. The only tangible fact uncovered was that due to the excessive drossing of the alloy, it was necessary to use indirect pouring. The bars with direct pouring were always badly drossed. Variation of metal or mold temperature did not seem to help at all.

When 85-5-5-5 brass castings were removed from the mold, they were found to have a thin, hard, black coating. It was difficult to remove this coating even with a file. It was found that it would loosen rapidly by immersing the parts in hot 15 per cent nitric acid, rinsing in cold water, drying with air blast, and then sand blasting the part. This produced a part with a very smooth finish. Probably this skin resulted from a reaction between the lead and the ceramic, since this problem was not encountered with lead-free alloys.

Lipson, Markus, and Rosenthal⁷ have shown that quite a range of mechanical properties can be obtained by quenching copper-base alloys by dumping the molds into cold water.

In casting 40E aluminum, one unusual fact was noted. In every case, test bars which had not less than 28,500 psi tensile after three days at room temperature achieved at least 32,000 psi after 21 days room temperature aging. Similarly, test bars which had not less than 3.75 per cent elongation after three days at room temperature had at least 3 per cent elongation after 21

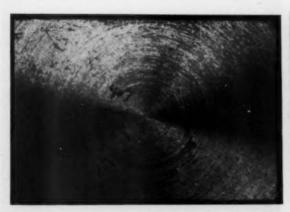


Fig. 15—Absence of pinhole formation at 760 C (1400 F) in 40E aluminum alloy. (× 2.19.)

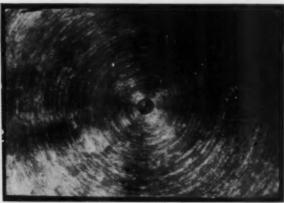


Fig. 16-Absence of pinhole formation at 980 C (1800. F) in 40E aluminum alloy. (× 2.19.)

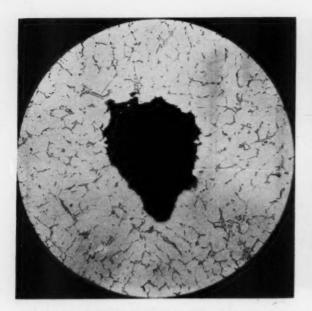


Fig. 17-Single pinhole which is typical of this defect in aluminum alloys.

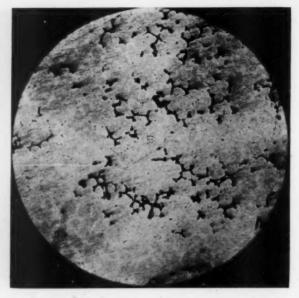


Fig. 18-Shrinkage due to lack of feeding in an aluminum alloy.

days room temperature aging. If the tensile strength after three days at room temperature was multiplied by 1.12 the result was within a few hundred pounds of the tensile which could be expected after 21 days at room temperature. If elongation after three days at room temperature was multiplied by 0.81, the elongation after 21 days at room temperature could be closely predicted. This permitted a chart to be drawn so that predictions could be made on the basis of three-day aging and it was not necessary to hold parts for 21 days before final testing and shipment. The chart is shown in Fig. 11.

Tests were made to see if gas pickup and pinhole

porosity tendencies were the same as for sand castings. The alloys retained the same characteristics except that conditions were exaggerated by the slow cooling. Heats of 355 and 40E aluminum were heated progressively from 1300 F (705 C) to 1800 F (980 C) with specimens poured at 100 F (55 C) intervals as the metal heated and cooled. Test blocks $1\frac{1}{2}$ in. x 1 in. x $\frac{1}{2}$ in. were cast and machined for visual examination and polished for micro-examination. The 355 alloy formed pinhole even when poured at 1400 F

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Fig. 19—Gas hole in aluminum alloy surrounded by microporosity.

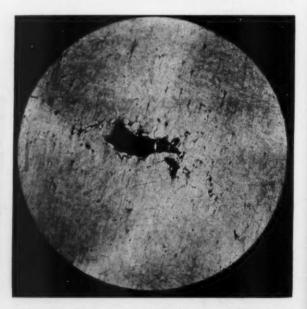
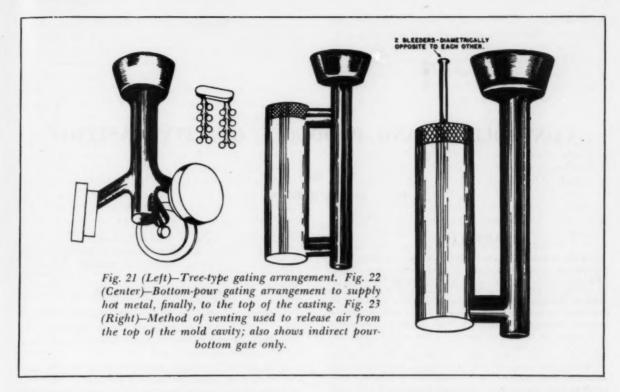


Fig. 20-Shrinkage area caused by poor feeding of an aluminum alloy.



(760 C) (Fig. 12). Holes were larger at 1800 F (980 C) (Fig. 13). After cooling to 1400 F (760 C) the holes were still present but more scattered and smaller than at 1800 F (980 C) (Fig. 14). The 40E showed no pinhole porosity at either 1400 F (760 C) (Fig. 15) or 1800 F (980 C) (Fig. 16). Micro-examination showed that the gas in 40E was present as microporosity which could be seen only by microscope or x-ray. Both alloys showed increased grain size as temperature increased and the original fine grain did not re-appear when the metal cooled.

Figure 17 shows a large pinhole. Note the rounded appearance all around the hole which is typical of pinhole porosity. Contrast this with Fig. 18 which shows shrinkage due to lack of feeding. In this case the riser broke off while still semi-molten and was unable to feed the casting. Note how the porosity tends to eat into the dendrites with finger-like formation. Figure 19 shows a large gas hole surrounded by many small, dark areas of microporosity. Figure 20 shows another shrinkage area in aluminum alloy caused by poor feeding. In this case there was an air leak and insufficient pressure was developed. Note again fingerlike structure of the shrinkage porosity.

Some effort was made to tie in the results of the tests with the gating of actual castings. It was found that for small castings it was best to use direct pour method by using tree type set ups which consisted of one or more down sprues to which were attached at a slight angle as many castings as could be found room for. Each casting was attached to the down sprue by a slender gate. (Fig. 21.)

For large castings 1/2 in. or more in thickness and

three or more inches in length, it was found best to use indirect pouring methods whereby the metal entered the casting at the bottom. At the top of the casting was placed another gate to allow escape of air during pouring and which acted as a "hot shot" at the end of the pour to supply hot metal at the top of the casting where it is needed during solidification. (Fig.

If bottom gating is not used on large castings and air vents not supplied at the top of the mold, air is likely to be trapped in the cavity and will appear as blows, non-fills or dendritic, crystalline, rough surface appearance on the castings. Sometimes the top gate may be omitted if adequate venting is supplied as shown in Fig. 23. Using this method of indirect pouring castings up to 11/2 in. thick and 6 in. long, or even solid elbow shapes, can be successfully cast.

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CONTROLLED SAND PRODUCES QUALITY CASTINGS

By

Bradley H. Booth*

ABSTRACT

It is the purpose of this paper to demonstrate the value to the foundryman of a standard control program for sand testing. First a brief review is made of the nature of sand, the reason for its use in the foundry and how it is used. Then a resume is made of the types of sand being used at eleven representative mid-western foundries, and the tests used at those foundries for control. A discussion of the better known sand tests is presented, with comments as to their practical value. Finally, the sand practice at several foundries is reported on in some detail in an effort to show how casting defects can be controlled by routine checking of the sand properties. The conclusion is reached that since experience has shown many casting defects to be the direct result of poor sand control, that every foundry will find it advantageous to regularly test their sand to help improve casting quality.

What Is Sand?

SAND is generally referred to as being a loose granular material resulting from the disintegration of rock. This means that there are many different kinds of sand, depending on the type of rock from which they were derived. For example, there are sand deposits composed almost entirely of such minerals as quartz, magnetite, calcite, dolomite, zirconite and olivine. However, quartz is the most common mineral in sands, principally due to its hardness, abundance and low solubility.

The name sand refers to size of grain and not to mineral composition. The diameter of individual sand grains may vary within the limits of 1.00 to 0.05 mm (approx. 18 to 270 mesh). Above this size range sand grades into gravel and below it into silt and clay.

For foundry use sands consisting principally of quartz (silica) grains are the most common. These sands may run as high as 99.7 per cent pure silica, and in some of the grades for molding, with a high clay content, the percentage of silica may run as low as 65 to 70 per cent. Sands high in silica are valuable for their refractory properties, while sand low in silica are useful because of their natural bond strength, and the fact that they may be obtainable with grain sizes

not commonly found in pure silica sand.

In this country the different types of foundry sands in general use may be classified as follows:

Foundry Sand Classification

- Silica—Over 95 per cent silica. Usually white in color, but may be tinted, due to impurities.
- Dune—Wind blown deposits usually found near large bodies of water.
- Bank—Sedimentary inland deposits of low clay content, generally under 5 per cent.
- Molding—Sedimentary inland deposits containing over 5 per cent natural clay, usually from 10 to 20 per cent.
- Miscellaneous—Includes zirconite and olivine sands and others not predominately made up of quartz mineral.

Why Is Sand Used in the Foundry?

Sands such as those mentioned in the last paragraph are used in the foundry to form the refractory mold which it is necessary to have in order to make solid shapes from the molten metal. Sand has been used for this purpose since the earliest dawn of the art of melting and casting metals, which was probably some 5000 to 6000 years ago. It is generally felt that copper was the first metal to be melted and, according to the historians, it was cast in open sand molds. However, because of the lack of knowledge of sand properties at that time, it was soon found more satisfactory to cast the metal in permanent molds cut out of sandstone or sun-baked clay. While these methods produced acceptable results with simple shapes, the demand for more complicated castings promoted the development and use of clay bonded sands as the mold material.

The universal use of sand for foundry molds has persisted through the years, largely because of its abundance, low cost and excellent refractory properties. Metal, cement and various refractory aggregates have all conspired to replace sand as the mold material, but in spite of this competition sand is still the principal ingredient in foundry molds. No satisfactory, economical substitute has ever been developed.

^{*} Foundry Engineer, Carpenter Brothers Inc., Milwaukee, Wis. Official exchange paper from the American Foundrymen's Society to the Institute of Australian Foundrymen for presentation at the 1949 Convention.

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How Is It Used in the Foundry?

Naturally bonded molding sands are probably the simplest to use in the foundry since the only treatment they require is the addition of sufficient water to make them moldable. After the initial use sands of this type can be prepared for further use by retempering and cutting over with a shovel or by mechanical preparation in one of the commercial sand cutting machines. If such a sand does not produce the quality casting desired then a specially prepared facing mixture may be used. In mechanized foundries the sand is generally subjected to more thorough control and preparation, including mulling, aerating and over-head handling.

Up until recent years natural clay bonded molding sands were almost universally used for molding. However, with modern knowledge of clay and sand properties it has been found advantageous in many foundries to use a so-called synthetic sand, made from some type or blend of sharp sands bonded with a fireclay or bentonite. Since synthetic sands should be prepared in a muller and tested frequently for best control, they are generally not recommended for the smaller shop unless mulling and testing facilities are available.

In the production of cores the dry bond is usually supplied by oil, resin, dry binder or other organic material, while green bond is obtained from clay in the sand or by the addition of other clay or cereal. Unlike molding sands, core sands can not be used in the natural state since for the purpose intended it is desired to have a high strength, perm, skin hardness

and low gas content. These properties are produced by mixing one or more of the core binders mentioned previously with the sand and baking in an oven until the desired properties have been developed. Although such core mixtures can be prepared by hand the use of either a core sand mixer or muller is dictated in the interests of economy and efficiency.

Foundry Sand Practice

In order to establish a background for our study of sand control it might be of value to review the sand practice at several typical American foundries. Certain pertinent data that were developed by such a review are tabulated in Table 1, for easy reference. In this table is listed general information regarding the principal molding and core sands in use at four gray iron, three steel, two mallcable and two nonferrous foundries. Under types of molding sand it will be noticed that within these eleven foundries there is approximately an even distribution in practice between natural and synthetic sands. However, with respect to the types of metal being cast this division is somewhat different. For example, the three steel foundries use synthetic sand (as do most steel foundries in this country), while the gray iron and malleable foundries appear to use both types with the synthetic sand being favored by the larger production shops. In the nonferrous field naturally bonded molding sands are still the favorite type. Sand control is important regardless of the type of sand being used, but since synthetic sands tend to be more sensitive to changes in moisture

TABLE 1-SAND PRACTICE AT TYPICAL AMERICAN FOUNDRIES

| | | Bas | se Moldi A.F.S. | ng San | ıd | | Base Cor A.F.S. | e Sand | | - |
|---------|------------------|----------------------|-------------------------|-------------|----------------|------------------|--------------------|------------|----------------|--|
| Foundry | Type of Metal | Туре І | Grain Grain Grain | | A.F.S. Perm | Туре | | | A.F.S. Perm | Sand Control |
| . A | Gray Iron | Synthetic | 50 | 0.2 | 190 | Silica | 70 | 0.3 | 115 | Moisture, Perm, Green Comp., and Grain Fineness |
| В | Gray Iron | Natural Synthetic | 33 80 | 17.0 0.2 | 500 110 | Silica Silica | 42 80 | 0.3 | 260 110 | Moisture Perm, Green Comp., Dry Comp., Dry Perm, Flowability, Grain Fineness, Clay, Baked Tensile and Core Hardness |
| C | Gray Iron | Natural Synthetic | 37 77 | 14.0 0.9 | 500 90 | Silica | 48 | 0.2 | 190 | Moisture, Perm and Green Comp. |
| D | Gray Iron | Synthetic | 93 47 | 1.4 | 75 220 | Bank Dune | 93 47 | 1.4 | 75 226 | Moisture, Perm, Green Comp., Cla and Grain Fineness |
| E | Steel | Synthetic | 60 | 0.5 | 150 | Silica | 60 | 0.5 | 150 | Moisture, Perm, Green Comp., Grain Fineness and Clay |
| F | Steel | Synthetic | 62 | 0.7 | 130 | Silica | 62 | 0.7 | 130 | Moisture, Perm, Green Comp., Clay Grain Fineness and Density |
| G | Steel | Synthetic | 45 | 1.3 | 195 | Silica | 60 | 0.5 | 150 | Moisture, Perm, Green Comp., Flow ability, Clay, Grain Fineness, Bakes Tensile and Hot Strength |
| н | Malleable | Natural | 121 | 16.0 | 32 | Silica Bank | 60 62 | 0.5 0.4 | 150 126 | Moisture, Perm and Green Comp. |
| I | Malleable | Synthetic | 50 | 0.0 | 300 | Silica | 50 | 0.0 | 300 | Moisture, Perm and Green Comp. |
| J | Nonferrous | Natural | 128 | 12.0 | 28 | Silica Bank | 70 76 | 0.3 | 110 90 | Moisture, Perm and Green Comp. |
| K | Nonferrous | Natural | 104 | 18.0 | 36 | Silica | 56 | 0.0 | 205 | Moisture, Perm, Green Comp., Clay and Grain Fineness |

and composition of the mix, it is of prime importance that they in particular be under close laboratory control.

With further reference to the molding sands mentioned in Table 1 it should be pointed out that the base sands for the synthetic type are either silica, dune or bank sands, generally with less than 1.0 per cent total clay content. The natural molding sands cover a wider range of properties, from the relatively fine nonferrous sands to the very coarse and open sands

for large gray iron castings.

In the section of Table 1 entitled "Base Core Sand" will be found silica, bank and dune sands. None of these sands are shown with more than 1.5 per cent clay, though occasionally there are core sands used that will run as high as 2.0 or 3.0 per cent. High clay in core sand is not to be recommended since it requires the addition of more core oil to maintain the same tensile strength as would be produced by a sand of lower clay content. It is also of interest to note that the degree of fineness of a core sand is not related to the type of metal being poured, but rather to the size and design of the casting.

The last column in Table 1 lists the various sand properties that are under control in each foundry. Although it will be noticed that the extent of sand control varies at each, the fact to remember is that they all recognize its importance and are using it to the best of their ability to help improve the quality

of castings being made.

From a further study of Table 1 it will be noticed that there is a considerable variation in the basic sand properties of the different sands in use at the eleven foundries. These variations can probably be explained by the fact that each foundry through the years has developed a particular sand practice best suited to the metal and type of castings being made at that foundry. Another factor to be considered is that, other things being equal, a foundry will tend to use the sands from deposits close to home, because of less transportation cost. However, the use of local sands may be false economy if a sand from a more distant point is better

suited for the job. After all, the value of foundry sands is measured in terms of a few cents per mold, whereas the cost of metal, labor and overhead is many times this. Reduction of scrap losses, improvement in casting finish and quality, that may be obtained by the use of the proper sand make the use of other and possibly cheaper sands uneconomical.

Production of Foundry Sands

The last two generations of American foundrymen have seen many improvements established in the production and processing of foundry sands. For example, at the turn of the century and for thirty years thereafter, the use of naturally bonded molding sands was prevalent in all types of foundries. These sands were dug from the pits and loaded on railroad cars with no preparation other than the incidental blending obtained by rehandling the sand. Today some molding sands are milled only while others may be screened and aerated; mulled and aerated; dried, milled, screened and aerated; or blended, dried, milled, retempered and aerated. Modern foundry sand production plants are equipped to produce molding sand to just about any specifications the customer may care to submit, as long as those specifications fall within the range of the grades that are available at the pits. To meet specifications closely it is common practice today to blend two or more sands in order more accurately to arrive at a specified clay content, grain fineness and permeability.

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Core sands also are being shipped today to specifications so close that 25 years ago they would have been thought impossible to meet economically. Where core sand used to be loaded wet right from the sand banks, with little or no control over clay, grain fineness or moisture, it is today being dried, screened and graded to exacting tolerances. In many cases a maximum clay content, particular grain fineness and moisture are

guaranteed by the sand producer.

Thus, controlled production of both molding and core sands has greatly reduced the problem of sand control at the foundry. There is no question but what

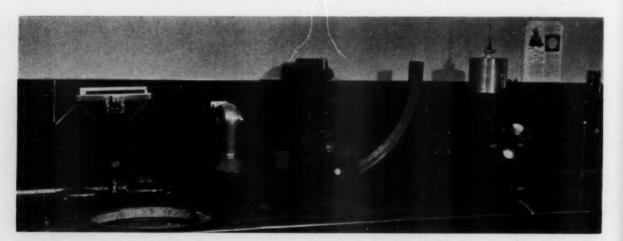


Fig. 1—Typical arrangement of testing equipment in a sand testing laboratory. From left to right, laboratory

balance, rapid moisture teller, universal strength machine and permmeter.

it is still a big problem, even though the technical data on sands and testing equipment that is available has greatly simplified the control of casting defects due to sand conditions.

Control of Sand Properties

What are the properties of sand that we are most interested in? It is, perhaps, not so much a question of which properties we are most interested in as those about which the most is known. With reference to molding sand the major properties that are receiving the most attention in foundries at the present time are:

1-Moisture

2-Permeability

3-Green Compressive Strength

4-Clay Content

5-Grain Fineness

(Dry compression, dry perm, shear strength, tensile strength, deformation, toughness, flowability and density are also accepted tests but less universally used in

shop sand control.)

Moisture—There is probably no more important test in foundry sand control work than the one for moisture. When it is considered that of the 31 casting defects listed in the A.F.S. book, ANALYSIS OF CASTING DEFECTS, that for 29 of them moisture can be a major or contributing cause, we begin to appreciate the great need for good control of this property in all sands. Fortunately, the test for moisture is one of the simplest and can easily be applied in any foundry regardless of size.

There are several popular test methods for moisture of which the following are probably the most widely used.

1-Oven Method

2-Rapid Moisture Teller

3-Speedy Moisture Test

4-Volumetric Moisture Test

5-Electric Moisture Test

In the *Oven Method* a sample of wet sand is weighed, dried to constant weight and the moisture calculated by difference in the wet and dry weights.

The Rapid Moisture Teller employs an electric heating coil through which air is blown by a fan. A 50-gram sample of sand is weighed into a brass pan with a fine wire mesh bottom. The pan is held be-

neath the heating element by spring tension and hot air blown through the sand for one minute. With proper arrangement of weights on the beam type balance the per cent moisture can be calculated direct from the loss in weight as indicated by the change in rider position.

In the Speedy Moisture Test the instrument operates on the gas pressure principle. This pressure is developed by mixing calcium carbide with a specified weight of moist sand in a metal bomb. The pressure developed is indicated by a dial gage which is calibrated in terms of per cent moisture.

The Volumetric Moisture Test employs the same principle except that instead of reading gage pressure the evolved gas is collected over water in a calibrated

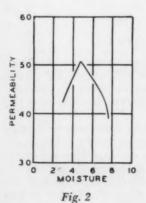
glass tube.

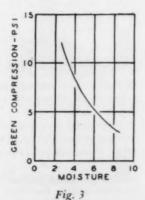
The *Electric Moisture Test* is based on the fact that moisture in sand changes its conductivity. One piece of equipment of this type has two metal arms with flat plates at the ends. When the plates are thrust into the sand and then compressed the per cent moisture can be read directly from a dial.

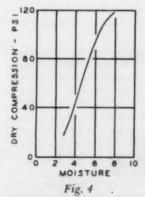
One reason for moisture being the possible source of so many defective castings is the tremendous change in volume that occurs when water is converted to steam at the boiling point. This expansion in volume is so great that should 1 cc of water be vaporized at 100 C (212 F) approximately 1678 cc's of saturated steam would be created.

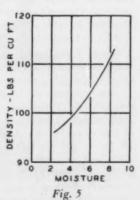
This great increase in volume produced by vaporization would seem to indicate that the moisture content of sands should at all times be held at the lowest practicable point. However, there are many other factors to consider, since moisture has a definite effect on practically every known sand property, such as green strength, dry strength, permeability and density. The effect of variations in moisture content on these properties of a natural molding sand is well demonstrated by the curves shown in Fig. 2 to 5.

The range of moisture values found in different foundries is quite wide. On the low side shops using a straight synthetic molding sand will usually operate at between 3 to 5 per cent moisture, while other shops using naturally bonded molding sands will tend to operate with moisture in the range between 6 to 8 per cent. Higher than 8 per cent moisture is not generally









Properties of Molding Sand vs Moisture

recommended, for reasons already mentioned, although it will be found that in foundries operating without moisture control the water content of heaps may frequently exceed that figure.

Optimum moisture in sand is largely controlled by the amount of clay bond, cereal, wood flour, and other water absorbing binders that are present in the sand. It is to be expected that a synthetic sand with 5 per cent bentonite would be properly tempered at a much lower moisture content than a naturally bonded sand containing 20 per cent clay.

Permeability—Permeability is defined as that physical property of the molded mass of a sand which allows gas to pass through it. It is expressed as the volume of air in cc's that will pass per minute under a pressure of one gram per square centimeter through a specimen one square centimeter in cross sectional area and one centimeter high.

In the standard A.F.S. method 2000 cc's of air under constant pressure is passed through a standard A.F.S. 2-in. cylindrical sand test specimen. The permeability can be calculated from the pressure and the time in seconds that it takes to pass 2000 cc's of air.

The routine shop control method makes use of standardized orifices attached to the permeability machine to regulate the air flow. With this method the back pressure created by the sand specimen is read from the pressure gage on the machine. This pressure reading can be converted to permeability by referring to the conversion table given in the A.F.S. FOUNDRY SAND TESTING HANDBOOK.

Permeability is important as a measure of the ability of a sand to allow quick passage for gases created when molten metal is introduced into the sand mold. For some types of metal, such as aluminum, permeability readings in the range from 5 to 10 are satisfactory. On the other hand, steel casting practice and heavy gray iron castings may require sands with permeability ranging all the way from 80 to 300, or even higher.

If permeability gets out of control blows, misruns, penetration, shrinks and other defects may result. Permeability is affected by many factors including grain fineness, grain distribution, moisture, pan material and ramming. Closely graded, uniform sands will tend to show the highest permeability for a given grain size, while high pan material and hard ramming will tend to reduce permeability rapidly. The moisture content of a sand at which the highest permeability is obtained is called the temper point for that sand with respect to permeability.

Green Compressive Strength—Green compressive strength of a foundry sand is the maximum compressive stress which a mixture is capable of developing.

This property of sand is measured by placing a standard A.F.S. test specimen in the compression apparatus and applying a load against the plane surfaces of the specimen so that the force is axial at a rate of $30~(\pm~5)$ psi per minute until the specimen breaks. The load at rupture is recorded as the green compressive strength.

Green strength is important as a measure of the ability of a sand to withstand the stresses that will be imparted to it during the making and handling of the mold. Sand that is too low in strength will not draw well from deep pockets and may drop from cope sections of the mold. On the other hand, sand that is too strong will not ram well, on account of its poor flowability, and this will quite likely result in a fluctuating mold hardness, soft spots and poor pattern impression.

As in the case of moisture and permeability, there is wide variation in the strength of sands in common use at different foundries. For example, nonferrous shops find that sands for their average type of work only have to have 4 to 6 psi green compressive strength, while some large gray iron shops feel that they must maintain strengths in the range from 14 to 18 psi in order to make satisfactory molds. This again illustrates the importance of sand testing and control if the properties of the sand are to be held within specified limits, which may have been found necessary in order to maintain uniform casting quality.

Clay Content—This property of sand is very important for imparting the required strength to the sand and also for helping to counteract the expansion of the silica grains, by its characteristic contraction when heat is applied.

A.F.S. clay is defined as those particles in a sand whose diameter is smaller than 20 microns. This classification does not draw a clear line between sand and true clay bond, since there will be particles of sand and silt which are less than 20 microns in size. However, it is the most widely used test for clay in foundry sands, and is the one that has been used in preparing this paper.

In the standard A.F.S. clay test a 50-gram sample of sand is mixed thoroughly with water and a deflocculating agent. This solution is then allowed to stand quietly for sufficient time so that all particles larger than 20 microns will have settled to the bottom of the container. The solution is then siphoned off to a point 1 in. from the bottom. Distilled water is added to bring the solution back up to the original level, the whole is stirred and allowed to stand for another time interval. This stirring and siphoning procedure is repeated until the solution above the sand is clear, at which point the 20 micron material is considered to have been completely washed off. The grain remaining is dried and weighed, the per cent clay being calculated as the difference in weight times two. A de-. tailed description of this test will be found in the A.F.S. FOUNDRY SAND TESTING HANDBOOK.

Other Tests for Clay

PERCENT RETAINED

Other tests for clay are the vibrating test developed by Eugene Smith, and the hydrometer and pipette methods. The hydrometer method is of particular value in classifying the sub-sieve fractions of a clay bonded sand.

The test for clay is of special importance in checking new sands and also for foundry control to help maintain a consistent percentage of clay in heap and system sands.

Two sands with the same clay content will not necessarily have the same strength and other properties. Much depends on the type of clay in each sand, its particle size and durability. For example, some sands from eastern United States have a large amount of silt in their clay so they are low in green strength. Other sands with similar clay content, but less silt, may show twice the strength under same conditions.

In actual foundry practice, the clay content of a sand should be known so that if occasion arises to make a change the starting point will be established and a definite increase or decrease in clay can easily be accomplished. Such changes may be desirable at any time for control of strength, permeability, sand expansion, flowability and other properties.

Grain Fineness—This is a basic property of every foundry sand and one which must be determined before any extensive control can be exercised over the other properties. It is determined by the standard A.F.S. procedure of taking a 50-grain representative sample, washing it free from clay, drying and then screening the remaining grain through a standard series of testing sieves recommended by A.F.S.

U. S. SIEVE SERIES FOR FOUNDRY MATERIALS

| U. S. Sieve | Mesh C | pening | |
|-------------|--------|--------|---|
| No. | In. | Mm | |
| 6 | 0.1320 | 3.360 | - |
| 12 | 0.0661 | 1.680 | |
| 20 | 0.0331 | 0.840 | |
| 30 | 0.0232 | 0.590 | |
| 40 | 0.0165 | 0.420 | |
| 50 | 0.0117 | 0.297 | |
| 70 | 0.0083 | 0.210 | |
| 100 | 0.0059 | 0.149 | |
| 140 | 0.0041 | 0.105 | |
| 200 | 0.0029 | 0.074 | |
| 270 | 0.0021 | 0.053 | |

FOR AVERAGE ALUMINUM CASTINGS

FOR AVERAGE GRAY IRON CASTINGS

FOR HEAVY GRAY IRON CASTINGS

FOR HEAVY GRAY IRON CASTINGS

6 12 20 30 40 50 70 100 140 200 270 PAN CLAY GRAIN DISTRIBUTION

Fig. 6-Screen analyses of typical foundry sands

These particular sieves were selected since they cover the size range of all sands. As mentioned previously, sands are generally considered to be loose granular material, the grains of which may vary between approximately 1.00 to 0.05 mm, or from 18 to 270-mesh sizes. Beginning with the 20-mesh screen in the above series, each succeeding screen has a mesh opening of one-half the area of the preceding screen, or a decrease in linear dimension by the square root of two. The first two screens vary by a larger ratio so as to allow for the separation of the grain in some of the coarser molding gravels.

The screen analysis of a particular sand tells us its grain size and the distribution of that grain. From the grain size it is possible to predict the type of finish that is likely to be obtained with that sand, and from the grain distribution an idea will be obtained as to the probable permeability of the sand and also its tendency to produce expansion troubles (rat-tails, buckles and scabs) or its probable density and ability to flow readily under ramming pressure.

With regards to grain fineness it may be simplest to demonstrate this property by showing typical screen analyses of sands being used for three widely different types of castings. (See Fig. 6.)

From the information given in Fig. 6 it can be easily seen that a screen analysis will quickly reveal the grain size of a sand, from which it can be decided what general class of foundry work it might be suitable for. However, if there happened to be two sands available of approximately the same grain size, then the distribution of the grain should be examined for the pur-

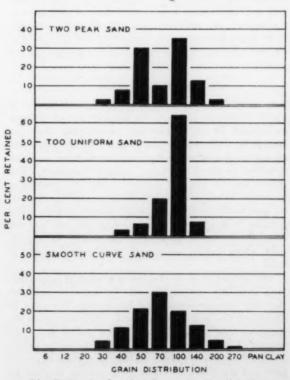


Fig. 7-Sands classified by grain distribution

pose of determining the relative merits of the two sands. For example, assume there are three sands as

shown in Fig. 7.

The "Two Peak Sand" shown in Fig. 7 is probably the most undesirable type of sand for foundry use. Due to its uneven grain distribution it will not promote the best casting finish, and because of its peculiar expansion and contraction properties, defects such as scabs and buckles are likely to occur. It will also tend to

have poor flowability.

The "Too Uniform Sand" is the type that tends to promote expansion defects because of its extreme uniformity in grain size. These uniform grains become rammed solidly against each other so that when heat is applied and expansion of the silica commences, there are not enough grains of other sizes to allow for a rearrangement in grain location so the surface of the mold ruptures and an expansion defect is developed. Uniform sands do have the advantage of producing the highest permeability for a given grain size, and can be successfully used if sufficient clay, wood flour, cereal or other buffing agent is present to compensate for the expansion of the silica grains. Thin, flat castings are more susceptible to expansion defects than castings of irregular shape.

The "Smooth Curve Sand" is strictly a theoretical sand, although there are a few known deposits where the sand will closely approximate this "smooth curve" type of distribution. In addition, this type of sand can be closely reproduced in practice by blending two or more commercially available sands. This class of sands have the advantage of a uniform variation in grain sizes such that sand expansion will take place with a uniform variation in grain sizes such that sand expansion will take place with a minimum of agitation at the surface. In addition, sands of this type will tend to be more flowable, give greater density and in general will produce a cleaner and neater draw from the pattern.

Sampling Precaution

It should be emphasized at this point that it is highly important in preparing sand for any of the tests mentioned in the preceding paragraphs, that the sample be truly representative and that the test specimen be prepared in strict accordance with the methods described in the A.F.S. SAND TESTING HANDBOOK. The reason for this word of caution is that the properties of tons of sand are being based on the results obtained on specimens made from a small sample of only a few pounds in weight. If these samples are not selected carefully it is quite possible to get widely varying results when testing supposedly duplicate batches of sand.

The preparation of the test specimen is important also, since the test results will be no more uniform than are the specimens used for testing. To assist in obtaining the best reproducibility, the interior of the specimen tube should be honed to a mirror finish and accurate 2-in, diameter. The rammer should be solidly mounted on a heavy cast iron base or equally firm support and all testing equipment should be in good repair and carefully calibrated.

Effect of Sand Control on Casting Quality

Now that we have established the nature of foundry sand, explained why it is used, how it is used and mentioned the sand practice at several typical foundries and described testing methods and their purpose, the next question is, "What is the value of this control in helping to produce better castings?" To answer this question it will perhaps be best to refer again to Table 1 and the practice in force at the several foundries mentioned there.

Take for example Foundry G, a jobbing shop engaged in the production of miscellaneous steel castings. From the chart it will be seen that they have in force

an extensive program of sand control.

It is their practice to make daily tests on their shop sands for moisture, permeability and green compressive strength. Less frequent tests are made for grain fineness, flowability, hot strength and baked tensile strength. Here is a typical sand mix in use at this foundry:

| System Sand, per cent | 50.0 |
|---------------------------------|------|
| Silica Sand (45 Grain Fineness) | |
| per cent | 50.0 |
| Bentonite (Western), per cent | 2.0 |
| Cereal (Corn Flour), per cent | 0.5 |
| Moisture, per cent | 4.5 |

Control of moisture in this mix is important in order to develop the best strength and flowability. High moisture promotes the formation of scabs and at low moistures the sand is not workable. In core sand mixes the moisture must be held below 4 per cent or the sand will not blow properly. In other mixes if all new sand is being used the moisture will be dropped to about 3 per cent or if dry sand molds are being made it will be increased to 6 or 7 per cent.

Permeability is maintained in the range between 180 to 240. In this particular shop high permeability is not a source of trouble, but too low a permeability may cause blows. If it is found necessary to add silica flour to the sand to help prevent penetration, the permeability of the sand will be greatly reduced and venting will have to be resorted to in order to help discharge the mold gases at a sufficiently rapid rate.

For the mix given a green compressive strength in the range between 7 to 8 psi has been found to work best. This property is controlled by varying the amount of bentonite added to the mix. If the strength is allowed to drop below this range, molders report difficulty in drawing the sand from pockets and drops from the cope can also be expected. Too strong a sand, on the other hand, is liable to decrease the flowability, makes ramming difficult and promotes soft spots on the mold surface.

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In addition to the above, the sand technician at this foundry reports that sand testing has a certain abstract value in that it tends to keep the molders happy, helps to provide facts for settling arguments and also keeps the mill man alert to producing sand mixes to specifications.

At another steel foundry (shown as E in Table 1) we find that on new sand arriving at the shop they make routine tests for screen analysis and clay content.

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This foundry uses just one grade of dried and screened silica sand for both molding and cores. This sand has an average grain fineness of 60. The grain is spread over five screens and the clay content is less than 1 per cent. The screen test is considered important as a means of control for casting finish and the clay test is used to insure keeping that constituent low enough so that oil consumption in the core room will not be excessive. Although it might be considered difficult to make a large variety of work using only one sand this shop reports good results on both light and heavy castings.

This type of practice does have the advantage of eliminating the problem of contaminating molding sand of one grain fineness with core sand of another grain fineness. The sand in use is the "Smooth Curve" type of sand referred to previously. The grain distribution of this sand is shown in Fig. 7.

This type of distribution is considered to be the ultimate for reducing expansion defects such as rattails, buckles and scabs.

Two typical sand mixes in use at this shop are given in the following paragraph:

SAND MIXES USED AT STEEL FOUNDRY 'E'

| | Турс | e of Mix | | | | |
|---|---|-----------------------|--|--|--|--|
| | New Sand Facing Old Sand Facing | | | | | |
| Material | (for bench and squeezer work) | (for bumper machines) | | | | |
| Silica Sand (60 gr. fi.) | 1000 lb | 500 lb | | | | |
| Heap Sand | - | 500 " | | | | |
| Bentonite (western) | 8 quarts | 6 quarts | | | | |
| Fireclay | 4 " | - | | | | |
| Cereal (corn flour) | 4 " | 3 quarts | | | | |
| Moisture | 2.5-3.0%* | 2.5-3.0%** | | | | |
| Phy | sical Properties | | | | | |
| Permeability | 90 to 115 | 90-115 | | | | |
| Green Strength | 5 to 51/2 psi | 7 to 8 psi | | | | |
| * To obtain moistur addition of from 2 t | | uires the | | | | |
| | ains heap sand, whice o add more water to evaporation. From e | allow for | | | | |

Heap sand for backing is rebonded with 6 quarts of fireclay per 1000 lb of sand. Even more than 4 gal of water must be added to this backing sand mix since all the sand is hot and the maximum amount of evaporation of moisture can be expected.

The shop sand mixes are tested daily for moisture, permeability and green compressive strength. The moisture test is considered important, especially for control on mixes made with hot sand where evaporation losses will be variable. In addition, it has been found that if moisture in the sand is allowed to get too low the sand will tend to be crumbly and the molds dirty. On the other hand, if high moistures are encountered the sand will be lumpy, and scabs and penetration defects can be expected. It is common practice to use scab nails to help eliminate this defect.

If green strength is allowed to exceed the limit mentioned the flowability of the sand will be reduced, it will be difficult to riddle and a poor pattern impression can be expected. Weak sand will be difficult to draw from the pattern and drop outs in the cope may occur.

Foundry 'H' is engaged in the production of miscellaneous malleable iron castings. Metal is melted in the conventional type of air furnace and cast in sand molds using synthetic facing and natural molding sand for rebounding heaps.

At this foundry routine tests are made on all carloads of new sand received or moisture, permeability and green compressive strength. The same tests are also run daily on the shop facing and heap sands. All facing sands are prepared by mulling in a high speed muller, while heap sands are wet down and then run through a sand cutter each evening. Facing is used on all jobs. The following sand mix is the facing used for all jobs up to 25 or 30 lb in weight:

LIGHT FACING (FOUNDRY 'H')

| Silica Sand (60 grain fineness) | 3 Shovels |
|----------------------------------|-------------|
| Heap Sand | 8 to 9 " |
| Fireclay (Illite type) | 1 to 2 " |
| Seacoal | 2 " |
| Moisture | 4.5 to 5.0% |
| Physical Properties | |
| Permeability | 35 to 40 |
| Green Strength | 7 to 8 psi |
| Mulling Time: 11/2 min | |
| NOTE: 1 shovel - approx. 20 lb | |
| Total weight of mix about 350 lb | |

For heavier castings from 30 to 40 lb up to 125 lb and 4-in. metal section, the following mix is preferred: (more silica sand is used in this mix to increase the refractoriness and reduce the tendency to burn in.)

HEAVY FACING (FOUNDRY 'H')

| Silica Sand (60 grain fineness) | 8 Shovels |
|---------------------------------|-------------------|
| Heap Sand | 2-3 " |
| Fireclay (Illite type) | 1 " |
| Bentonite (western) | 1 " |
| Seacoal | 3 " |
| Moisture | 4 to 4.5% |
| Physical Propert | ies |
| Permeability | 40 to 45 |
| Green Strength | 9 to 10 psi |
| | igh speed muller) |

It has been found from experience that if the moisture in either of the preceding mixes is allowed to exceed the prescribed limits, the castings will quite likely have a rough finish and also be subject to blows. Low moisture is seldom encountered as the sand will not be workable and molders will complain. However, if it does happen to drop below the minimum limits there is liable to be trouble from cuts and washes.

Permeability has not been found to be a problem in this foundry with the sands that they are using, even though on occasion it has dropped as low as 25. The metallurgist accounts for this lack of permeability trouble to the fact that they use low moisture and a good sand grain distribution.

Green strength also is quite easy to control with the bentonite and Illite type clay they are using. Also the fact that all molds are faced and all facing is mulled



Fig. 8-Blow defects caused by high moisture. Sand should have contained between 4.0 and 4.5 per cent water, but test revealed 7.0 per cent.

is a good indication that it should not be difficult to control strength at almost any desired vaue. As in other foundries, sand that is too strong will not ram well and sand that is too weak will not handle or draw well.

The chief casting defect encountered in this shop is "hot tears." This is the common problem in any foundry casting white iron, since this type of metal has a higher shrinkage than gray iron and less strength than steel. "Hot tears" can be quite successfully controlled by using less of the Western bentonite and more of the Illite clay, or by addition of southern bentonite or wood flour. In fact, anything that will reduce the hot strength of the sand will help in the control of this defect.

Sand Testing in Gray Iron Foundry

To continue with the investigation of foundry sand control the sand practice at Foundry 'B,' a large gray iron foundry, will be investigated next. At this foundry all incoming carloads of sand are tested to see if they meet specifications. Molding sand is checked for moisture, permeability, green strength, dry strength, clay and grain fineness. In establishing the physical properties of new sand tests are made at the same moisture as is commonly used with that sand in the foundry. Core sands are checked for grain distribution only.

In actual foundry use all shop facing and heap sands are tested regularly for:

- 1. Moisture
- 2. Permeability
- 3. Green Strength
- 4. Flowability
- 5. Dry Strength
- 6. Dry Permeability
- 7. Core Hardness

(The last three tests are used principally on pitch bonded black core sands.)

Ordinarily each box of sand taken from the muller is sampled and tested. When making facing sand each batch is tested, while sand used in the slinger is sampled and tested once each hour. Facing sand is used on all jobs in this shop coming in the range between 50 and 2500 lb. A typical facing sand mixture for this class of work is given in the following paragraph:

FLOOR FACING SAND

| I LOOK I MOING ON | |
|--|---------------------------------|
| (Gray Iron, 50 to 2500 | lb) |
| Silica Sand (75 grain fineness) | 20 Shovels |
| Reclaimed Sand | 20 " |
| Bentonite (Western) | 6 Quarts |
| Black Fuel Oil | 2 " |
| Permi-Bond | 71/2 " |
| Seacoal | 71/2 " |
| Water 4.0 to | 1.5% (approx. 21/2 gals.) |
| Note: 1 Shovel = Approx. 1 gal. (Note: This mix must be mulled for the fing for less time is liable to product tendency to cause scabbing, possibly lack of uniformity in the sand mix tendency is noticeable even at 12 mi. Physical Properties | on account of a . This scabbing |
| Permeability | 90 |
| Green Strength | 11 to 12 psi |

This sand should be rammed hard and the mold sprayed with a touch-off spray. This mix is tested regularly for moisture, permeability, green strength and flowability.

The molding sand used for light work (up to 50 lb) at this shop is made up as follows:

MOLDING SAND FOR LIGHT WORK

| (Grav Iron- | -Up to 50 lb) |
|---------------------------|--------------------------------|
| Reclaimed Sand | 40 Shovels |
| Bentonite (Western) | 1 Quart |
| Black Fuel Oil | 1 Pint |
| Water | 4.0 to 4.5% (approx. 11/2 gal) |
| Mull: 5 to 7 min | |
| Physical | Properties |
| Permeability | 90 |
| Green Strength | 11 to 12 psi |
| The Black Fuel Oil me | ntioned in the above mix |
| is not used every day. T | The principal control of oil |
| addition is by visual exa | amination of the mold and |
| the casting. Too much w | thite smoke during pouring |
| | il while poor casting peel |
| | e too little oil in the sand. |

Moisture is watched closely in this shop. Experience has shown that low moisture will promote scabbing and other expansion defects, while high moisture tends to produce blows. With the sands being used the per-

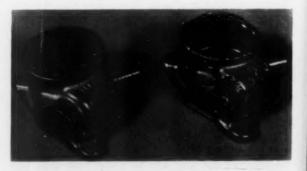


Fig. 9-Blow defects caused by combination of high moisture and too much pan material. Both castings made in same sand, one on left at 8.2 per cent moisture and one on right at 10.1 per cent moisture. Pan material-31.4 per cent.

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sho sand mor meability runs quite uniform and no recent trouble has been noticed that could be ascribed to this source.

Strength of the molding sand is regarded as quite critical and every attempt is made to hold it within the prescribed limits. It has been found that when the sand is too weak there is likely to be trouble with scabbing on flat, light work; or veining, rat-tailing and buckles in the corners on heavier work. It is also difficult to get a good draw from the pattern on floor work. Low strength also means that loose surface sand will be blown off the mold when spraying. Since weaker sands are generally lower in clay this means they are also more subject to metal penetration and burn-in.

Sand Flowability

High strength sands, on the other hand, tend to have poor flowability and consequently are difficult to ram. Casting finish may be rougher, and on heavier floor work so-called 'pregnant' castings may be produced, on account of swelling in the mold. There is also likely to be shrinks, and due to the sand being rammed in layers veining may be evident on casting surfaces.

The flowability test is considered a good one in this shop for controlling the amount of oil in the sand. It has been found that best results are obtained with flowability in the range between 85 and 90. If the flowability drops below 85 the sand feels dead, is not workable and will not draw well from the pattern. This trouble can be overcome by increasing the amount of oil in the sand.

Another type of sand that is widely used in this shop is a black core sand, which is used for large cores from about one to six feet square and weighing up to 1½ tons. Here is a typical mix:

BLACK CORE SAND

| Millville Gravel (35 grain fineness) | 20 shovels |
|---|------------|
| Pitch | 21/2 gal |
| Reclaimed Sand (Hydroblast) | 40 shovels |
| Water (11/2 to 2 gal) | 7.5% |
| Mull in high speed muller for 1½ Physical Properties | min |
| Dowmonbilis | 100 |

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Pan

Permeability 120
Green Compressive Strength 8 to 10 psi
Dry Compressive Strength 250 to 280 psi

In making cores with this mix it is common practice to use lots of rods. However, the controlling factor in sand of this type is the dry strength. If it is too low there will be trouble with broken cores and possibly cutting and washing. On the other hand, if the dry strength is too high castings are liable to be cracked, core buckles created and a difficult shake-out problem encountered.

Among the chief casting defects at this point are metal penetration and burning in. Experience has shown that these defects are often caused by either, (1) Too long a pouring time, or (2) Too low a clay content in the sand.

As was mentioned earlier in this paper, nonferrous shops in general still tend to use natural molding sands. However, even these less sensitive sands are more reliable when placed under technical control, and for that reaon many nonferrous shops are turning to sand control as another step in their efforts to make more and better castings.

At Foundry 'K,' a nonferrous shop making a large variety of alloys, all new sand coming in to the foundry is tested for:

- 1-Moisture
- 2-Permeability
- 3-Green Compressive Strength
- 4-Clay Content
- 5-Grain Fineness

Facing and heap sands in this shop are tested daily for:

- 1-Moisture
- 2-Permeability
- 3-Green Compressive Strength

Moisture is considered one of the more important properties to control. The biggest problem is to keep it low enough. The sand is seldom too dry and when it is tempered on the wet side trouble can be expected from blows, rough finish, scabs and related defects.

Permeability runs fairly uniform when using the same sands, but should it drop too low blows are quite likely to occur.

Green strength is also fairly easy to control, as long as sufficient additions of new sand are made to the heaps. If, for any reason, the sand does get too weak then good molding will be difficult, casting finish rougher and clean draws from the pattern will not be easy.

Another type of defect that is common in this shop is spongy surfaces or slag inclusions, generally caused by a drossy condition in the metal. This condition of the metal is often created by wet sand, since it will promote turbulence in the molten metal. Here is another good reason for moisture control.

Summary

1. What is sand? Sand is a loose granular material resulting from the disintegration of rock.

2. Why is it used in the foundry? Sand is used in the foundry to form the refractory mold which it is necessary to have in order to make solid shapes from the molten metal. Its use for this purpose has persisted through the years largely because of its abundance, low cost and excellent refractory properties.

3. How is it used in the foundry? Naturally bonded molding sands can be used simply by tempering with water and mixing thoroughly. Synthetic sands are prepared by mulling sand, clay and other ingredients in a muller with water, until the desired strength and other properties have been developed.

4. Foundry sand practice. A resume has been given of the sand practice at several foundries, including a brief description of the types of molding and core sands being used. It was also pointed out that while steel foundries favor the synthetic type of sand and nonferrous foundries the natural sands, both types are still popular in gray iron and malleable shops.

5. Control of sand properties. The most common sand properties under control at the present time were listed as:

1-Moisture

2-Permeability

3-Green Compressive Strength

4-Clay

5-Grain Fineness

A brief description was given of each property, methods of making the tests and reasons for their usefulness in helping to improve casting quality.

6. Effect of sand control on casting quality. In this section the sand control program at several of the foundries mentioned in Section 4 was discussed in more detail. This discussion brought out the following relationships between sand control and casting quality:

A-REASONS FOR SAND TESTS

Moisture—For development of optimum properties in the sand, and control of casting defects that may be caused by the moisture being too high or too low.

Permeability—To measure the ability of a sand to allow quick passage for mold and core gases, thus preventing blows and other defects.

Green Compressive Strength—Control of this property is important to insure the ability of a sand to withstand mold pressures and yet be flowable and easy to ram.

Clay-To help control strength and expansion of molding sands and oil absorption in core sands.

Grain Fineness—To help control casting finish, permeability, expansion and other sand properties.

B-SAND TESTS IN ACTUAL USE

In reviewing the sand testing practice at the several foundries mentioned it will be noted that the five tests referred to in Section 5 were the only ones being used on both new and shop sand, with the exception of dry strength, which was mentioned as a control test on new sand by one foundry. The tests reported in common use by the various foundries were as follows:

| On New Sand | On Shop Sand | | | | | |
|---|---|--|--|--|--|--|
| *1-Moisture *2-Permeability *3-Green Strength | *1-Moisture *2-Permeability *3-Green Strength | 7-Dry Permeability 8-Flowability 9-Baked Tensile | | | | |
| *4-Clay *5-Grain Fineness *6-Dry Strength | *4-Clay *5-Grain Fineness 6-Dry Strength | 10-Core Hardness 11-Hot Strength | | | | |
| *-These are the to | | text as being the most sand practice. | | | | |

C-FOUNDRY DEFECTS DUE TO SAND PROPERTIES

Low Moisture

Sand not workable Sand too crumbly Dirty molds Cuts and washes Scabs

High Moisture

Metal turbulence

Rough finish

Blows

Scabs

Metal penetration

Lumpy sand

Sand will not blow properly

Low Permeability

Blows

Low Green Strength

Drops from cope

Sand difficult to draw

Veining

Rat tails

Buckles

Scabs

Crumbly mold surface

Metal penetration

Burn-in

High Green Strength

Poor flowability

Ramming difficult

Soft spots

Difficult to riddle

Poor pattern impression

Rough finish

Swells

Shrinks

Veining (sand rammed in layers)

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Conclusions

1. Sand is a crude material and as such must be controlled carefully both in production and use, if casting quality is to be of the best.

2. Sand control tests have been developed to help provide some of this essential control. There is a reason for each test, the problem is to see that they are used.

3. It has been demonstrated that when sand gets out of control foundry defects will be prone to occur.

4. Every effort should be made to control sand properties at those values which will tend to hold foundry defects to a minimum.

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FERROUS MELTING FURNACES IN THE UNITED STATES AND CANADA

By A. W. Gregg*

THE FOUNDRY INDUSTRY has been a vital factor in the industrial development of the United States. Foundries have been obliged to expand and to mechanize all possible operations in order to satisfy the growing demands of industry, particularly the railroads, automotive plants, builders of agricultural machinery, the mining industry, the public utilities, and the manufacturers of plumbing and sanitary ware.

The great distances in the United States from north to south and from east to west have created tremendous transportation problems necessitating thousands of miles of railroads and millions of automobiles, buses, and trucks. The great central area, with its large-scale farms, could not be operated efficiently without farm machinery such as tractors, harvesting machinery, trucks and various kinds of soil tilling machinery.

Many foundries have mechanized and modernized operations, and in so doing have increased the productive capacity of individual plants. Considering both the increase in number of plants and added capacity provided by modernization, it seems probable that the foundry industry in America now has facilities to produce greater tonnages than ever before.

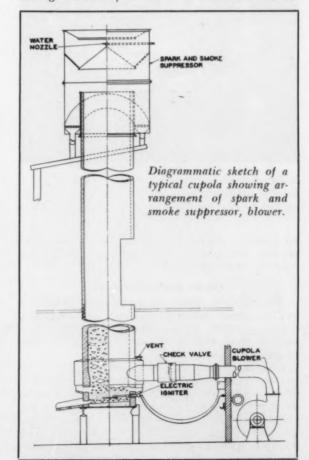
Capacity of all foundries is estimated at 15 to 16 million net tons of castings per year. The industry employs some 400,000 people, and the annual payroll is about one and one-quarter billion dollars. The total annual value of castings produced in the United States is something over 4 billion dollars.

The influence of the American Foundrymen's Society has been most constructive. Cooperative research and exchange of information have had a tremendous influence in the development of improved foundry practice, better quality castings and lowered costs. The Society, for over 50 years, through its literature and promotion of exhibits of new equipment has stimulated the advance of the industry. One of its earliest projects was the development of an apprentice training program which has been backed up annually

for many years with a contest for molder and pattern maker apprentices.

The A.F.S. Sand Research Committee has been in existence for nearly 40 years and has developed standards and methods of testing foundry sands which are internationally accepted.

Manufacture of chilled iron car wheels furnishes an excellent example of a highly mechanized foundry serving the railroads and specializing on one kind of casting. In four years—1945 to 1948 inclusive—car



^{*} Formerly Consulting Engineer, Whiting Corp., Harvey, Ill., now deceased.

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Official Exchange Paper from the American Foundrymen's Society to the International Foundry Congress was presented at the Meeting held in Amsterdam, Holland, Aug. 29-Sept. 2, 1949.

wheet toundries shipped 12,932,000 wheels, having a total weight of 4,744,000 net tons. This is an average of 1,186,000 net tons of car wheels shipped per year.

Gray Iron Foundry Industry

The oldest and still the largest branch of the foundry industry in America is that manufacturing gray iron castings. The earliest iron casting made in America was cast in 1642. This was a cooking vessel manufactured by the Saugus Iron Works, near Lynn, Mass. The metal was made in a crude blast furnace capable of producing about 8 tons of pig iron per day.

Gray iron foundries in the United States and Canada number 3,457, of which 373 are in Canada. These foundries operate 4,979 cupolas having a total estimated hourly capacity of 36,569 net tons. Of these foundries, 25 per cent employ mechanical chargers, and 75 per cent of the cupolas are still hand charged. Forty per cent of all cupolas are over 20 years old. About 40 per cent of the gray iron foundries have



A 10-ton cradle furnace, fired with powdered coal, is used in duplexing practice. Cupola metal may be tapped directly into the furnace, or delivered by ladle.

sand testing laboratories, about 30 per cent have physical testing laboratories, and about 35 per cent have chemical laboratories.

Cupola Melting Furnaces

The cupola is almost the universal melting unit, and the annual weight of iron melted in cupolas in a good year is 15 to 16 million net tons. This represents a production of about 12 million net tons of gray iron castings.

Cupolas are built with shell diameters of 26 to 130 in., and diameters inside the lining range from 17 to 90 in. or more. Cupola melting capacities are based on 10 lb per hr per sq. in. of area inside the lining, and the melting rates are from one ton to 30 or 35 tons per hour. Heights range from 30 to 90 feet from grade to top of stack. Tuyere area compared to area inside the lining varies from 10 to 25 per cent. The number of tuyeres varies from four on a small cupola to 16 on a cupola having a shell diameter of 130 in.

Coke is the favored cupola fuel, and is almost universally used. During the past two or three years when coke was in short supply, some foundries used a per centage of anthracite coal.

Naturally, the cupola melting loss is extremely variable, depending upon the character and cleanliness of the metallic charge. With clean scrap and pig iron and good operating conditions, the melting loss may be as low as 2 per cent. With oxidized, dirty or light-section scrap, melting loss may be as high as 10 per cent. With a large percentage of steel scrap in the charge, the melting loss will be about 6 per cent.

Other Furnaces for Gray Iron

Although the cupola produces about 98 per cent of all gray iron melted in the United States and Canada, a small tonnage of cast iron is made in electric furnaces, and the greater part of the iron used for chilled rolls is made in open hearth or air furnaces.

The electric furnace is used for melting special and alloy grades of cast iron. Some of the manufacturers of individually cast piston rings have always used the electric furnace.

Because coke in the United States since the war has been at an all-time high in cost, and quality has been much below the standard of pre-war years, some gray iron foundries are considering electric melting, and several have installed small electric furnaces to superheat cupola metal. Duplexing with cupola and electric furnace has an advantage over cold-melt electric furnace practice because it maintains the advantage of a continuous supply of molten metal.

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Cradle Furnace

Used in connection with cupola melting the cradle furnace serves as a heated mixing and distributing ladle. Metal temperature can be maintained or slightly increased, and uniformity of composition is greatly improved.

Cradle furnaces are built in sizes ranging from 3 to 25 tons holding capacity. They are usually fired with pulverized coal. The tilting mechanism is power operated. Cupola metal can be tapped directly into the cradle furnace, or delivered to it with a ladle if the furnace is located at some distance from the cupola. The pouring spout is located on one side of the furnace. A pour-in spout and a skimming opening are located on the opposite side. The refractory material used for side walls and roof consists of silica brick or high temperature duty clay brick. Monolithic rammed bottoms are popular. Roofs are bung type.

Mechanical Charging

Undoubtedly one of the greatest improvements in cupola operation is due to the introduction of mechanical charging equipment. There are many types, ranging from the simple vertical skip hoist capable of serving a single cupola and usually employed on smaller sizes, to the elaborate complete systems serving a number of cupolas of any size.

The make-up system in the stock yard for a complete installation employs a traveling crane with a magnet, an iron loading platform with a weigh hopper, and a coke and stone bin with a weigh hopper, from which these materials are delivered by gravity into the charging buckets. A transfer car is used to

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per, vity l to move the loaded buckets from the make-up point to the pick-up point under the charging crane. Instead of a traveling crane for charging, some installations employ an inclined skip hoist which may serve one or more cupolas.

A completely mechanized charging installation employs from two to three men and can handle up to 40 tons per hour. In some plants, 20 men or more

have been replaced by three.

Each installation should be individually engineered to comply with local requirements and conditions. Complete mechanical charging installations not only cut labor costs, but also eliminate hard manual labor and provide safer and more pleasant working conditions. They also encourage greater accuracy and better housekeeping.

Steel Foundry Melting Furnaces

In the United States and Canada, there are about 300 steel foundries making commercial castings, of which 22 are so-called captive foundries. According to the Steel Founders' Society of America, the industry in a normal year ships between 900,000 and one million net tons of castings. In the peak year of 1942, production was 1,679,178 tons. Railroads consume 35 per cent of the product, machinery and tools 25 per cent, rolling mills 15 per cent, and the remaining 25 per cent is scattered among many different industries. Upward of 20 per cent of the steel foundries' output is alloy steel of various compositions.

Electric arc-furnaces are by far the most used melting furnaces, especially for the production of small and medium size castings. In 1948, 43½ per cent of total tonnage produced was made in electric furnaces. The open-hearth furnace is used for large tonnages and heavy castings. The side-blow converter is used to produce steel for light castings of thin sections. The crucible furnace was the first steel melting unit for the production of small steel castings and had this

field almost entirely to itself prior to the introduction of the side-blow or Tropenas converter into the United States about 1890.

The electric arc-furnace had its first big start in the steel foundries of America during World War I, and continued to expand its field during World War II. In 1948 about 425 electric arc-furnaces were in use in the steel foundries of the United States and Canada. These furnaces range in holding capacity from 500 lb to 50 tons; 61 per cent are less than 10 years old. The 2-to 3-ton furnace is the size in most general use in foundries specializing in small and medium size work.

Most of the furnaces built during the past 5 years have been of the top-charge type. Top-charge furnaces reduce the time between heats to a minimum, with a consequent saving in power and an increase in production. Also, a saving is made in yard labor as charges are generally made up with a magnet yard crane.

The majority of electric funaces in steel foundries are operated with acid linings. This was customary before the war because in ordinary times low phosphorus, low sulphur scrap was in good supply. During the last war, however, some 65 foundries were obliged to change to basic practice when low phosphorus scrap was practically unobtainable.

Triplexing Operation

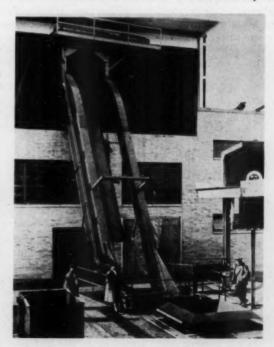
An interesting process, developed and operated by a large steel foundry in Michigan, is known as triplexing. The charge, consisting largely of steel scrap, is melted in a cupola. After desulphurizing in the ladle with alkali slags, the metal is blown in a side-blow converter. The converter metal is deoxidized in the ladle with manganese, silicon and aluminum, and delivered continuously to an electric furnace which has a holding capacity of about three converter blows and is kept reasonably full.

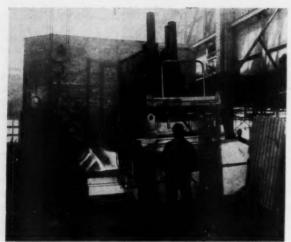
Metal from the electric furnace is delivered to pour-



Vertical skiphoist charger installed in a small foundry. Charges are made up by hand and moved to skip bucket in wheelbarrows.

Swivel-type ship hoist capable of charging two or more cupolas. Charges are made up with a magnet crane and delivered to hopper shown in foreground.





ing ladles suspended on a mono-rail, and is poured into molds on a moving conveyor. The electric furnace in this process functions as a heated distributing ladle. Metal temperature and carbon content are checked after each delivery of metal from the converter. Power consumption in the electric furnace is about 50 kilowatt hours per ton. This process has interesting possibilities for continuous high capacity production of steel castings. It makes possible in a steel foundry the benefits of continuous molding and shakeout operations.

According to the directory of the Steel Founders' Society of America, there are 121 open-hearth furnaces in 45 steel foundries in the United States and Canada. Seventy-one per cent of these furnaces are over 20 years old, and only 8 per cent are under 5 years.

Open-hearth furnaces in foundries range from 10to 50-tons capacity. The size in most general use has a capacity of 30 tons. Oil is the most popular fuel, with natural gas of 1000 B.T.U. per cubic foot in use where price is favorable and supply dependable. Some furnaces are fired with a mixture of oil and gas, and a few are fired with producer gas.

Open-hearth furnaces produce about 50 per cent of the total tonnage of steel castings. The yield of castings compared to molten metal varies from 50 to 75 per cent.

Open-hearth furnaces are economical only for large tonnages and heavy castings and, for best results, should be kept in continuous 24-hr operation.

Side-Blow Converter

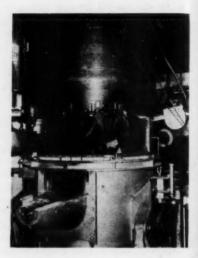
The first Tropenas side-blow converter to make steel for castings was installed in a foundry in the city of Milwaukee, Wis., in April, 1899. The side-blow converter soon replaced the crucible furnace for the manufacture of small and medium size steel castings because of its lower operating costs and ability to produce steel of high temperature in large quantities.

At one time there were over 50 foundries in the United States making steel castings with the side-blow converter, and from about 1900 until World War I it was the favored process for the production of steel for small castings.

At the present time, according to the directory of

A 6-ton electric-arc furnace used in duplexing practice in production of alloy cast iron.

Charges for the top-charge type of electricarc furnaces are usually made up by a magnetyard crane. Charging time is reduced and production increased.



the Steel Founders' Society of America, there are 21 foundries in the United States and Canada operating 32 converters. The vessels range in size from 1-ton to 6-ton holding capacity. The size in general use has a holding capacity of 2 tons. All converters at present are operated with acid lining. The positive impeller type of blower is used almost universally. The usual rating for a converter blower is based on 1400 to 1600 cu ft per minute per ton of holding capacity, at pressures ranging from 4 to 5 lb.

Molten metal for the converter charge is supplied by cupolas, the composition being about 3 per cent carbon, 0.60 to 0.70 per cent manganese, and 1.50 to 2.00 per cent silicon. The cupola charge consists largely of steel scrap. Because the converter operates with acid lining, phosphorus and sulphur in the charge must be within the specified limits for the finished steel.

Cupola metal is desulphurized with soda-ash and caustic soda. With careful practice, upward of 70 per cent of the sulphur in the metal can be removed with certainty.

Practically all converters in America have rammed monolithic linings composed of silica ganister and a binder. A few operators use silica brick. Lining life is variable, depending upon the care taken in ram-

Molten metal for the 2-ton capacity side-blow converters is supplied by the cupola shown at the left.



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ming and drying. Fifty to 100 blows can be made on a lining with proper practice, and some foundries have made as many as 200 blows on a lining.

High Frequency Induction Furnaces

The high frequency electric furnace has been gaining steadily in popularity during the past 10 years, although the first production unit was not installed in the United States until 1930. It is used principally for the production of alloy steels, and especially for alloy steels of the corrosion and heat resistant types.

In the United States, 35 foundries are now operating 88 induction furnaces, ranging in capacity from a few pounds to 2000 lb, but the usual size in the production foundries rarely exceeds 1000 lb. Transformers are usually rated on the basis of 800 kw per ton of furnace holding capacity. These furnaces are lined with either acid or basic refractories. The time of melting varies with the size of the furnace, the size of the charge, and the power input.

Heats are made from clean, selected scrap containing alloys of known composition. The furnace charge must be of good density. As the change in composition is exceedingly small during the melting operation, steel can be made with accuracy to a given specification. In most operations, no slag is used. The induction furnace must be classified as a steel "melting" unit rather than "refining" unit.

Numerous alloy compositions are required in the chemical and other industries, and quantities ordered may be small. The rather small capacities of the induction furnaces in operation are admirably adapted for such service. High alloy scrap of known composition can be used in the charge without loss of alloys through oxidation.

Malleable Iron Industry

All of the malleable iron castings made in America are of the well-known blackheart type, which was discovered and developed by Seth Boyden at Newark, The high-frequency electric furnace is used principally in high alloy steel production.



N. J., during the years 1826 to 1832. Boyden, whom Thomas Edison called "one of America's greatest inventors" started his experiments with the idea of duplicating the whiteheart malleable iron developed by Reamur in France about 100 years earlier and commonly produced in Europe today.

In the early days of the industry in America, malleable iron was melted either in crucible furnaces, or in a small and very crude type of cupola. A low-silicon charcoal pig iron was charged and high-carbon white iron produced. Small air furnaces of 800- to 1000-lb holding capacity soon supplanted the cupola and the crucible furnace. Melting and annealing practices were based on rule of thumb, and any improvements were regarded as trade secrets and zealously guarded as such.

Today, under the influence of the Malleable Founders' Society, there is a free interchange of information, even among competitors, and all melting and annealing operations are conducted under scientific control.

In the United States and Canada, over 100 found-

Malleable foundry duplexing operation—cupola to p o w d e r e d coal-fired air furnace. Coal bin and pulverizer are shown at the right.



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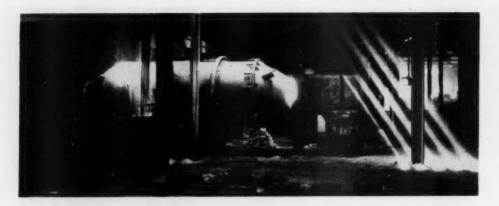
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The Brackelsburg furnace is a rotary type of air furnace, generally powdered-coal-fired. The installation shown is a 5-ton furnace for a malleable duplexing operation.

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ries are making malleable cast iron, with production in a normal business year of one million net tons. Fifty per cent of this tonnage is consumed by the automotive industry, and most of the remainder is used in about equal amounts by the railroads and the manufacturers of agricultural machinery. The malleable foundries operate a total of about 250 air furnaces, and 50 per cent of the foundries are making castings with metal produced by the cupola-air furnace duplexing process.

Sixty to seventy per cent of the total tonnage of malleable iron is now made by the duplex process, and the greatest number of installations involve cupola to air furnace equipment. Grade A malleable iron is readily produced by this method. At least 90 per cent of all air furnaces are now fired with powdered coal. A few air furnaces are fired with oil, and a few are still hand-fired with coal.

One of the foundries using the cupola to electric furnace process has a rated capacity of 90 tons of melted metal per hour, and in peak periods operates 16 hr per day. This foundry has produced in excess of 650 net tons of finished castings per 16-hr day. With continuous operations, the cupola-electric furnace process produces Grade B malleable iron, but if operated as a batch process can also produce Grade A.

The advantages of the continuous duplexing process may be listed as follows:

1. Because this process supplies metal continuously, it makes possible the advantages of continuous mechanized molding and pouring practice.

2. Maximum production from a minimum of floor space and a minimum of personnel.

3. Higher efficiency in practically all foundry departments due to specialization of pouring, shifting and shakeout operations.

4. Opportunity to improve housekeeping and working conditions, which tends to attract better workers.

Successful Use of Undesirable Melting Stock

During the war, foundry operators in all branches of the industry were obliged to charge melting stock which a few years earlier would have been rejected

after visual inspection. By careful control and the use of alloy additions, the foundries have been successful in maintaining the quality of their various products. Iron foundries found it necessary to improve desulphurizing practice.

Use of borings in the cupola charge was made possible by the development of efficient briquetting machines, which produced easily handled briquettes having very high density. Large hydraulic presses for making bundles from light-gage sheet metal made satisfactory melting stock for open hearth and electric

The present high state of development in the foundry industry is largely the result of extended research, not only by scientific and trade organizations, but by the foundry operators' themselves, and free exchange of information between foundrymen. Free interchange of ideas and continuous search for new facts and improved methods is the path to future advancement in this basic industry.

Acknowledgments

The author wishes to express his appreciation for the assistance accorded him in the preparation of this paper. This came from friends in the foundry industry, and he is especially indebted to the following: S. C. Massari, technical director, American Foundrymen's Society; C. W. Briggs, technical director, Steel Founders' Society of America; James Lansing, technical director, Malleable Founders' Society; W. R. Jaeschke, Whiting Corp.; Anthony DeYoung, Whiting Corp.; Erle Ross, Penton Publishing Co.; and the technical staff of Whiting Corp.

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TRENDS IN ALUMINUM CASTING ALLOYS

By

Walter Bonsack*

CASTING ALLOYS OF ALUMINUM are used in ever increasing amounts. This increase is due to several factors. With time the structural and economic value of this metal has been more and more recognized, and more engineers and foundrymen have become acquainted with the metal. But also, the introduction of newer alloys and the evolution which older alloys have gone through made the metal easier to handle in the foundry. The expansion of the use of aluminum in die casting is a typical example. Aluminum alloys are cast mainly in sand, permanent mold and die casting, but increasing quantities are being consumed by other casting processes such as plaster molding, centrifugal and investment molding.

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Early American alloys were like the European, the aluminum-zinc-copper alloys. However, since these alloys are inherently hot short they soon lost their standing in favor of the aluminum-copper alloys.

New Alloys Developed

For many years the 8 per cent copper alloys represented the bulk of aluminum casting alloys. For aluminum pistons and bearings 10-12 per cent copper alloys were used extensively. With the discovery of heat treatment the 4 per cent copper alloy was introduced. Aluminum-silicon alloys began to appear gradually; 13 per cent and also 5 per cent were the common casting alloys of this series. The addition of magnesium to these latter alloys made them heat treatable and more useful. So at the end of the 1920's there were two distinct groups of aluminum casting alloys—the aluminum-copper alloys and the aluminum silicon alloys.

The use of silicon alloys gradually increased because they are considerably less hot short and have much better casting characteristics than either the aluminumzinc-copper or the aluminum-copper alloys. However, they presented a change in foundry technique and machining properties and therefore the 8 per cent copper was still the favorite alloy for most common castings.

Silicon alloys were particularly suited for die castings because of their lack of hot shortness and their high fluidity, and it can safely be said that they were the basis for increased production of die castings.

It may be said that with the knowledge of hot shortness in copper alloys and the lack of hot shortness in silicon alloys, it was pure logic to combine the two and produce a more nearly ideal casting alloy. This may have been the case, or it may have happened that the two alloys or the scrap of the two alloys became mixed accidently. The fact remains that by adding silicon to copper alloys better fluidity was gained and hot shortness decreased with the increase in silicon content. This evolution, of course, was not spontaneous, and even after the facts were recognized the acceptance of these alloys was very slow.

First, the permanent mold caster realized the advantages of the silicon addition and specified alloys containing silicon. It enlarged the field of his activity considerably. Silicon was added to the 8 per cent copper (Curve A, Fig. 1), and also new alloys ap-

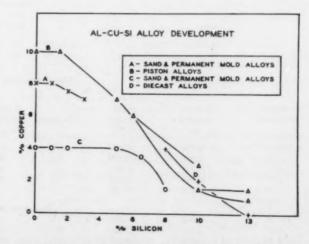


Fig. 1-Tendencies in development of Al-Cu-Si alloys.

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Presented at the 23rd Annual Foundry Congress, in Paris, Oct. 10-11, 1949, as the Official Exchange Paper of the American Foundrymen's Society to the French Technical Foundry Association

TABLE 1-TYPICAL PROPERTIES OF ALUMINUM CU-SI ALLOYS AS-CAST

| Cu, | | Sand Cast | | | l Cast | Die Cast | |
|-----|--------|-----------------------------|---------|-----------------------------|---------|-----------------------------|--------|
| | Si, | Tensile Strength, psi | Elong., | Tensile Strength, psi | Elong., | Tensile Strength, psi | Elong. |
| 4 | 1 max. | 22,000 | 3.0 | - | | _ | _ |
| 4 | 2 | - | - | 30,000 | 2.5 | - | _ |
| 4 | 3 | 24,000 | 2.5 | 27,000 | 3.0 | - | - |
| 4 | 5 | 22,000 | 2.0 | 28,000 | 2.0 | 40,000 | 5.0 |
| 3.5 | 6 | 27,000 | 2.5 | 33,000 | 3.5 | _ | _ |
| 4 | 8 | 26,000 | 2.0 | 32,000 | 2.5 | 45,000 | 2.5 |
| 2 | 8 | 27,000 | 2.5 | 32,000 | 2.5 | | - |
| 3.8 | 12 | | - | - | _ | 46,000 | 1.0 |
| - | 12 | | - | - | | 39,000 | 2.0 |

peared containing 4 per cent copper and 2, 3, and 5 per cent silicon. Figure 1 gives an indication of the tendencies which were developed. For example, the 10 per cent copper alloys were the original piston alloys. They suffered from nearly the same lack of hot strength as the 8 per cent copper alloy. Again, silicon additions gave superior casting alloys.

At first up to 2 per cent silicon was added with good success, but later larger additions improved castability and incidentally also reduced the thermal expansion. To compensate for the rapid increase of hardening by the larger silicon addition, logically the copper had to be reduced. The evolution was therefore 10 per cent copper-2 per cent silicon, 7.5 per cent copper-5 per cent silicon, and finally 6 per cent copper-6 per cent silicon (Curve B, Fig. 1). Aluminum piston alloys containing still higher silicon will be discussed later.

This tendency had been established before the last war. The smelters of aluminum scrap have contributed considerably to the economic utilization of the ever increasing amount of industrial and domestic aluminum scrap. The effects of the most common alloying elements had been studied carefully.¹ The proper balancing of these elements, even in minor amounts, for good casting alloys had been established fairly well, and when the scarcity of commercially pure aluminum and the abundance of aluminum-copper alloy scrap from airplane production during the war forced greater use of copper-containing aluminum alloys, the smelting industry was well prepared.

Duralumin scrap was the main source of raw material for casting alloys. Since it contains 4 per cent copper, only the excess magnesium had to be removed and silicon added to suit the casting process in question. Four per cent copper-2 to 3 per cent silicon was used for heat treatable permanent mold castings; 4 per cent copper-3.5 per cent silicon for general sand castings, and 4 per cent copper-5 per cent silicon for permanent mold and die casting.

An alloy containing 3.5 per cent copper and 6.0 per cent silicon was later developed, an excellent sand and permanent mold casting alloy which is also heat treatable and developes very good properties. Just before the war a similar alloy but with lower copper and higher silicon had been introduced. This alloy contains 1-2 per cent copper, 8 per cent silicon and some magnesium for heat treatability.

In the die casting alloy field the trend was somewhat of the same order. As stated, pure aluminum was not available for the high silicon alloys. Copper-bearing raw materials had to be used. This resulted in the development of alloys containing 10 per cent silicon-2 per cent copper, 9 per cent silicon-3 per cent copper, and 8 per cent silicon-4 per cent copper. These latter alloys have now become the bulk of the die casting alloys. They not only have excellent casting properties but also better machining qualities than the original 13 per cent silicon alloy. The Curve "D" in Fig. 1 indicates this trend.

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In all of these alloys it appears that the economic picture has demanded the change in alloy composition.

TABLE 2-COMPOSITION AND PROPERTIES OF SAND CAST AND PERMANENT MOLD CAST SC 8 AND SC 9 ALLOYS

| | | | | | Composit | ion | | | | |
|-----|----|---------|-------------|---------|-------------|-------------|-------------|-------------|-------------|------------------|
| All | oy | Cu | Fe, max. | Si | Mn, max. | Mg, max. | Zn, max. | Ti, max. | Ni, max. | Others, total |
| SC | 8 | 3.3-4.3 | 1.0 | 5.5-7.0 | 0.5 | 0.1 | 1.0 | 0.2 | 0.3 | 0.5 |
| SC | 9 | 3.0-4.5 | 1.2 | 5.5-7.0 | 0.8 | 0.5 | 1.0 | 0.2 | 0.5 | 0.5 |

| | | Typical Properties — Sand Alloy SC 8 | | | Alloy SC 9 | | | | |
|--------------------|-----------------------------|---|-------------|-------------------------------------|-----------------------------|----------------------------|-------------|-------------------------------------|--|
| Heat Treatment* | Tensile Strength, psi | Yield Strength, 0.2% | Elongation, | Hardness Brinell, 500 kg load | Tensile Strength, psi | Yield Strength, 0.2% | Elongation, | Hardness Brinell, 500 kg load | |
| As-Cast | 27.000 | 16,000 | 2.5 | 65 | 28,000 | 16,000 | 2.0 | 70 | |
| T-4 | 38,000 | 28,000 | 3.0 | 90 | | | | | |
| T-6 | 35,000 | 24,000 | 3.5 | 85 | 36,000 | 24,000 | 2.0 | 80 | |
| T-7 | 39,000 | 30,000 | 1.5 | 90 | | | | | |
| T-51 | 29,000 | 18,000 | 2.0 | 70 | | | | | |
| | | | Perma | nent Mold Cast | | | | | |
| As-Cast | 33,000 | 19.000 | 3.0 | 70 | 34,000 | 19,000 | 2.5 | -85 | |
| T-4 | 41,000 | 23,000 | 4.5 | 90 | 40,000 | 27,000 | 3.0 | 95 | |
| T-61 | 50,000 | 42,000 | 1.5 | 100 | | | | | |
| T-51 | 37,000 | 25,000 | 2.0 | 70 | | | | | |
| T-7 | 40,000 | 24,000 | 4.0 | 90 | | | | | |
| * See Table 3 | for heat treatme | nt designations. | | | | | | | |

Others

This is partially true; the pressure was in this direction, but it has only speeded up the acceptance. The development was going very certainly in this direction. The better castability was the basic factor. However, the physical properties are also quite attractive to the designing engineer.

Table 1 shows a few of the properties of the alloys discussed in the foregoing. This table also indicates in which type of casting method they are used.

Casting Method Affects Tensile Strength

It is interesting to note that the tensile strength in sand casting alloys ranges from 22,000 to about 27,000 psi, and the elongation centers around 2.5 per cent. By casting these alloys into permanent molds higher strength and equal or better ductility is obtained; die casting gives still better strength. It should particularly be noted that the 12 per cent silicon alloy die cast has generally lower properties than the copperbearing lower-silicon alloys.

An alloy which has been greatly used since the war is that containing 3.5 per cent copper-6 per cent silicon. It was developed during the war. Considering its two main alloying elements only, it should not show any exceptional properties. However, as in most of these aforementioned alloys, a control and balance of the other alloying elements present determines not only the casting, but also the other physical properties. Tichy² reported on the development of this alloy in 1946. This alloy and a similar one were later incorporated in the ASTM Specifications B 26-48T and B 108-48T as Alloy SC 8 and Alloy SC 9, respectively.³

The difference between these two alloys is not too obvious (Table 2). It should be pointed out therefore that alloy SC 8 is considerably more closely controlled in the alloying elements, copper, iron, manganese, and magnesium. Magnesium particularly is held to a low limit in order to give the alloy greater ductility. As it is known that magnesium causes age hardening even at room temperature, alloys with magnesium higher than 0.15 per cent lose ductility at a fairly rapid rate. However, by keeping the magnesium to such low

TABLE 3-COMMON HEAT TREATMENTS

| | | Solu Treat | | Aging Treatment | | |
|-------------------|-----------------|---------------|-------------|--------------------|-------|--|
| Alloy | Treat- ment* | Temp., | Time, hr | Temp., | Time, | |
| CS 5; SC 8 & SC 9 | T-4 | 500-510 | 4-12 | - | _ | |
| CS 5; SC 8 & SC 9 | T-6 | 500-510 | 4-12 | 145-155 | 2.6 | |
| CS 5; SC 8 & SC 9 | T-7 | 500-510 | 4-12 | 220-235 | 7-9 | |
| CS 5; SC 8 & SC 9 | T-51 | - | - | 220-235 | 7-9 | |
| SC 42 | T-4 | 505-516 | 4-12 | _ | _ | |
| | T-6 | 505-516 | 4-12 | 145-155 | 3-5 | |
| | T-61 | 505-516 | 4-12 | 145-155 | 12 | |
| | T-7 | 505-516 | 4-12 | 220-235 | 8 | |
| | T-51 | - | - | 220-235 | 7-9 | |
| 34; 66; Red X-10; | | | | | | |
| Red X-13; Red X- | 20 T-52 | - | - | 195-205 | 8 | |

^{*} T4-Solution heat treated and quenched.

Alloy Cu Si max. Zn max. max. max. (each)

Fe.

Mn.

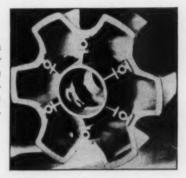
Ni.

TABLE 4-COMPOSITION AND PROPERTIES OF CS 5 ALLOY

Mg,

| CS 5 | 3.5- 4.5 | 2.5- | 0.05 | 1.0 (1.0-2.0) | 1.0 | 0.6 | 0.3 | 1 | |
|---------|-------------|-----------|-----------|------------------|---------|-------------|-----|-------------|--|
| | | | Typical i | Propertie | s — Sa | nd Cast | | | |
| | | Tensile | | Yield | | | | Hardness | |
| Heat | | Strength, | | Strength, | | Elongation, | | Brinell, | |
| Treat | | psi | | 0.2% | | % | | 500 kg load | |
| As-Cast | | 24,000 | | 14,000 | | 2.5 | | 60 | |
| T-4 | | 30,000 | | 18,000 | | 4.0 | | 65 | |
| T-6 | | 36,000 | | 26,000 | | 1.5 | | 85 | |
| T-51 | | 28 | ,000 | 21,000 | | 1.5 | | 75 | |
| | | | Pern | nanent / | Mold Co | nst | | | |
| As-Cast | | 27 | ,000 | 16,000 | | 3.0 | | 65 | |
| T-4 | | 39 | ,000 | 18,000 | | 5.5 | | 90 | |
| T-6 | | 40 | ,000 | 26,000 | | 4.0 | | 85 | |
| T-51 | | 35 | ,000 | 28,000 | | 1.0 | | 80 | |

Fig. 2—Aluminum alloy truck trailer wheel was cast in a permanent mold one of the many heavy-duty applications.



limits as in alloy SC 8 the danger of embrittlement is well removed while yet retaining sufficient magnesium to produce response to heat treatment, good yield strength, and a minium lowering of ductility. The closer control of copper also adds to these factors. The manganese is balanced against the iron to prevent the ill effects of high iron on casting properties. Thus an alloy has been produced which equals or even betters some of the older heat treatable casting alloys.

The heat treatment designations used in the tables and tests are based on the following scheme adopted in the United States:

T-4: Alloy solution heat treated and quenched.

T-5 (T-51): Artificially aged casting without previous solution heat treatment.

T-6 (T-61, etc.): Solution heat treated and artificially aged.

T-7: Solution heat treated and then stabilized at elevated temperatures.

The temperatures and time used in the various heat treatments change, of course, with the various alloys and with the size of the castings, the mode of fabrication, and the properties desired. Most common treatments are given in Table 3.

A similar alloy but with lower silicon consists of 4 per cent copper-3 per cent silicon specified by ASTM as Alloy CS5.³ Its composition and physical properties are given in Table 4. This alloy is used in sand and permanent mold casting in preference to the older no. 12 alloy or 8 per cent copper. It is also heat treatable. Zinc in this alloy is usually limited to 1.0

T5 (T-51)—Artificially aged without previous solution heat treatment.

T6 (T61, etc.) –Solution heat treated and artificially aged. T7–Solution heat treated and then stabilized at elevated temperatures.

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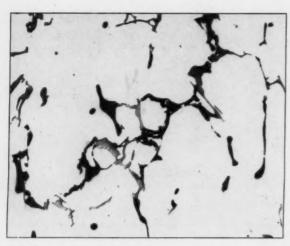


Fig. 3-Microstructure (above) of alloy CS 5 (4 per cent copper-3 per cent silicon) before heat treatment. Chill cast; etched in 25 per cent nitric acid. 200X. Right-typical casting made of the CS 5 aluminum alloy.

per cent maximum, but good results have been obtained with up to 2.0 per cent zinc.

The structure shown in the photomicrograph of Fig. 3 gives the constituents of the alloy before heat treatment. Mainly visible is the black aluminum-copper compound, the gray silicon crystals, and in fine needle form the aluminum-iron complex. During solution heat treatment most of the copper compound will go into solid solution and will not be visible.

A third type of alloy which has been used successfully during the past 10 years, built up from the same raw materials, contains 1.5 per cent copper and 8 per cent silicon, and is known as SC 42. In its composition manganese and magnesium are closely controlled within given limits to assure the properties. The high silicon content assures good castability in sand as well as in permanent molds for complicated castings. During the war it was used for V-8 tank engines (Fig. 4).

Cylinder blocks, crank cases, cylinder heads and

TABLE 5-SC 42 ALLOY-COMPOSITION AND PROPERTIES

| | | | | Compo | sition | | | |
|----------------|------------|------------|-------------|--------------------|--------------------|--------------------|------------|-------------------------|
| Alloy SC 42 | Cu 1.0- | Si 7.0- | Mg. 0.2- | Zn, max. 1.0 | Cr, max. 0.3 | Fe, max. 0.8 | Mn 0.2- | Others (each) 0.2 |
| | 2.0 | 8.5 | 0.6 | | | | 0.8 | max. |

| Prope | rties — Sand | Cast | - |
|-----------------------------|---|--|--|
| Tensile Strength, psi | Yield Strength, 0.2% | | Hardness Brinell, 500 kg load |
| 27,000 | 16,000 | 2.5 | 60 |
| 33,000 | 20,000 | 2.5 | 80 |
| 30,000 | 21,000 | 1.5 | 70 |
| 37,000 | 25,000 | 2.5 | 85 |
| 42,000 | 37,000 | 1.0 | 90 |
| 36,000 | 28,000 | 2.0 | 80 |
| Perm | anent Mold C | ast | |
| 32,000 | 19,000 | 2.5 | 70 |
| 40,000 | 21,000 | 5.0 | 80 |
| 36,000 | 28,000 | 1.0 | 90 |
| 44,000 | 27,000 | 4.0 | 85 |
| 49,000 | 40,000 | 2.0 | 90 |
| 41,000 | 28,000 | 3.0 | 85 |
| | Tensile Strength, psi 27,000 33,000 30,000 37,000 42,000 36,000 40,000 40,000 44,000 49,000 | Tensile Strength, psi 0.2% 27,000 16,000 33,000 20,000 30,000 21,000 37,000 25,000 42,000 37,000 28,000 Permanent Mold C 32,000 19,000 40,000 21,000 36,000 28,000 44,000 27,000 49,000 40,000 | Strength, psi Strength, 0.2% Elongation, % 27,000 16,000 2.5 33,000 20,000 2.5 30,000 21,000 1.5 37,000 25,000 2.5 42,000 37,000 1.0 36,000 28,000 2.0 Permanent Mold Cast 32,000 19,000 2.5 40,000 21,000 5.0 36,000 28,000 1.0 44,000 27,000 4.0 49,000 40,000 2.0 |



other auxiliary castings were made from this alloy. Its composition specification and typical properties are given in Table 5, and the properties are quite flexible due to the heat treatability of the alloy.

By increasing the silicon content of SC 42 alloy, and varying the magnesium and manganese contents various low-expansion piston alloys have been produced. During the war even the copper content was raised somewhat with no deleterious effect. The composition of these alloys and the physical properties are given in Table 6.

Of these three low-expansion piston alloys the first is the one most used, in spite of the fact that it has the highest expansion. The reason for this is its excellent castability as compared to the other two, and better machinability.

Figure 5 shows the typical microstructure of a permanent mold cast red X-10 alloy. It is of the same order as the alpax alloys showing aluminum dendrites in a matrix of aluminum-silicon eutectic. The dark needle-like component is the iron complex compound, while the "Chinese script" like crystals are the manganese-iron-silicon complex compounds. Copper is

TABLE 6-LOW EXPANSION PISTON ALLOYS

| | | | (| Compositi | on | | | |
|----------|------|---------------|------|-------------|-------------|------|------|------------------|
| Alloy | Cu | Si | Mg | Zn, max. | Fe, max. | Mn | Ni | Others (each) |
| Red X-10 | 1.0- | 9.5- | 0.4- | 0.6 | 0.8 | 0.5- | - | 0.2 max, |
| Red X-13 | 1.0- | 11.0- 13.0 | 0.4- | 0.4 | 0.9 | 0.5- | 5 | 0.2 max, |
| Red X-20 | 1.0- | 19.0- 22.0 | 0.4- | 0.5 | 0.9 | 0.2- | 0.3- | 0.2 max, |

| | | Typica | Propertie | | | |
|--------------------------|---------------|-----------------------------|----------------------------|-----------------|----------------------------|---|
| Alloy | Heat Treat | Tensile Strength, psi | Yield Strength, 0.2% | Elonga- tion | Brinell, 500 kg load | Coefficient Thermal Expan- sion* |
| Red X-10 | (T-52) | 40,000 | 34,000 | 0.5 | 95 | 20.9 |
| Red X-13 | (T-52) | 40,000 | 32,000 | 0.5 | 100 | 20.1 |
| Red X-12 * 10-6in./ir | (T-52) | 30,000 | - | - | 110 | 17.8 |

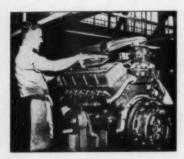
in solid solution and not visible in the structure.

With increase of silicon the expansion of the alloy is lowered, as indicated in the last column of Table 6, but at the same time the thermal conductivity is also lowered. By lowering the conductivity the piston will naturally run hotter in the engine, and the advantage gained by lowering the expansion is greatly lost due to higher operating temperature.

The high silicon gives excellent bearing surfaces, but makes the casting more difficult to machine. Also, alloys containing 20 per cent silicon require high pouring temperatures, which slows production considerably. All the factors discussed must be considered under our mass production methods, and if these factors do not add up favorably, no matter how good the alloy it will be used only in limited specific applications. This is the case with the 20 per cent silicon alloy.

Since the war another piston alloy has found large application. The alloy known as 34 or D 132 is also based on the utilization of airplane scrap, but in this case, engines. Its composition and typical properties are given in Table 7.

Although the specification is rather widely written, in actual practice it is kept much closer to allow control of castability and also of properties. It is used in competition with the 6 per cent copper-6 per cent silicon alloy at present, although the latter alloy is



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Fig. 4-Complicated castings, such as those on the V-8 tank engine shown during assembly are cast in the high-silicon aluminum SC 42 alloy.

easier to cast and to machine. (The 6 per cent copper-6 per cent silicon alloy is also used in France.) The properties of both alloys are closely comparable or even better for the 6 per cent copper-6 per cent silicon alloy, as shown in Table 7.

The structure of this alloy (Fig. 6) is very similar to that of alloy SC 5 shown in Fig. 3, only the copper compound is present in large amounts. Since the copper content is greater than the limit of solid solution, considerable amounts of copper will be visible even after solution heat treatment. This copper compound contributes materially to the bearing quality of the piston alloy.

In the same family of alloys belong the die casting alloys previously mentioned; the 4 per cent copper-5 per cent silicon, 4 per cent copper-8 per cent silicon, and 2 per cent copper-10 per cent silicon alloys. The 4 per cent copper-8 per cent silicon alloy is the most widely used, and the 4 per cent copper-5 per cent silicon, although the oldest, is used the least. The ASTM Specification B 85-48 T specifies the composition limits for this alloy as shown in Table 8.

Two alloys are specified for two different uses. The

TABLE 7-PISTON ALLOYS 34 OR D132, AND 66

| Alloy 34 | Cu 2.0- | Si 8.5- | | Zn, max. 0.5 | Fe, max 1.2 | Mn, max. 0.5 | Ni 0.5- | 1 | |
|-------------|------------------|------------|--------------------------|--------------------|---------------------------|--------------------|------------|------------------------------------|--|
| | 4.0 | 10.5 | 1.5 | | | | 1.5 | | |
| Alloy | Heat Treat | | Tensil Strengt psi | e h, St | yield rength, 0.2 % | Elor tio | n, | Hardness Brinnell, 0 kg load | |
| 34 66 | (T-51) (T-51) | | 36,000 40,000 | | 28,000 31,000 | 1.0 | | 100 100 | |

lower iron containing alloy for cold chamber, high pressure die casting machines, and the higher iron alloy for gooseneck machines. Although two different zinc limits are given for the two alloys, the zinc has no bearing on the properties, the castability, and the usefulness of the alloy, nor does it conflict with type of die casting machine used, and the differentiation must be disregarded as wholly useless.

The 4 per cent copper-8 per cent silicon alloy has excellent fluidity and is not hot short. It does not solder to the die and has excellent all-around casting qualities. As shown in Table 1, it has good mechanical properties and it machines well. It can be used wherever corrosion is not a problem. An interesting application is shown in Fig. 7.

These castings are for hydraulic pressure pump control valves.⁴ The castings must withstand 1800 psi pressure without leaking. This is remarkable since in the past die castings have suffered from a reputation for porosity. But here it is shown that by applying the proper technique and care, with the proper alloy, a reliable, strong casting can be produced by the die casting method.

In contrast to alloys SC 6 and SC 7, a newer series of alloys has been introduced in the past 10 years which attain physical properties of the previously discussed aluminum alloy without necessarily resorting to heat treatment. However, if a solution heat treatment is applied they will develop properties far beyond those of the older or commonly used alloys. These alloys are essentially of the Al-Zn-Mg type.



Fig. 5-Typical microstructure of permanent mold cast red X10 aluminum alloy. Etched, 20% H₂SO₄. 250X.

TABLE 8-COMPOSITION LIMITS-ALLOYS SC 6 AND SC 7 (ASTM SPECIFICATION B 85-48 T)

| Copper | | 3.0-4.0 |
|--------------|-----|----------------|
| | | |
| Silicon | | 7.5-9.5 |
| Manganese | | 0.5 max. |
| Magnesium | | 0.1 max. |
| Zinc | | 0.6 or 1.0 |
| Nickel | | 0.5 max. |
| Tin | | 0.3 max. |
| Others (tota | al) | 0.5 |

These alloys as stated develop their high strength by aging at room temperature over a period of several weeks. After casting they usually have very high ductility and lend themselves readily to cold forming, bending or straightening without cracking. Generally speaking, the strength immediately after sand casting is around 30,000 psi and the elongation around 10 per cent. Specifically, the first two alloys have properties as shown in Table 9.

The hardening element in these two alloys is a complex Al₂Mg₃Zn₃ compound. The ternalloy 5 alloy does not materially improve with solution heat treatment, while ternalloy 7 alloy can be solution heat treated and high properties obtained. The heat treatment for the latter alloy consists of 4-16 hr at 538 C for solution treatment, and 3-5 hr at 145-155 C aging for T-6 treatment, and 4-10 hr at 170-185 C for the T-7 treatment. However, desirable properties are obtained without treatment, as shown in Table 9.

Both alloys are mostly used in the untreated condition. Heat treatment and heat treating equipment are expensive, and since the alloys possess high properties even without such treatment, they are of particular advantage to foundries which do not have such facilities for processing castings.

The alloys have other desirable properties such as excellent machinability and corrosion resistance. They are dimensionally very stable and do not grow under the influence of heat, as do most aluminum alloys during aging. They take a high polish and are of silver-white color.

Compared to the micrographs of the aluminum-copper-silicon and aluminum-silicon-copper alloys, the structure shown in Fig. 8 appears like one of a pure metal. Only a few particles of highly dispersed compounds of the minor alloying elements are visible. The high solubility of the major alloying elements causes the high strength and high ductility.

With all of these advantages there must, of course, be some disadvantages. There are two which should be mentioned: alloys of aluminum-magnesium, aluminum-zinc, and aluminum-magnesium-zinc are as hot short as the aluminum-copper alloys. Therefore, they are not as easily cast as is the case with the aluminum-copper-silicon alloys.

Special care must be taken to overcome this hot shortness by the usual foundry techniques applied for such alloys. Furthermore, silicon will destroy the high properties of these alloys very rapidly, and care must be taken to prevent silicon contamination in the

Fig. 6-Chill cast alloy 66. Etched, 25% HNO 2. 250X.

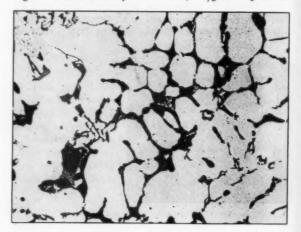


TABLE 9-ALUMINUM, ZINC, MAGNESIUM TYPE ALLOYS

| | | | | Compositio | n | | | |
|---------------|-----------------|-----------------|----------------|------------|----------|----------|----------|--------|
| Alloy | Mg | Zn | Mn | Cr | Ti | Cu | Si | (each) |
| Ternalloy 5 | 1.4-1.8 | 2.7-3.3 | 0.4-0.6 | 0.2-0.4 | 0.2 max. | 0.2 max. | 0.2 max. | 0.05 |
| Ternalloy 7 | 1.8-2.4 | 4.0-4.5 | 0.4-0.6 | 0.2-0.4 | 0.2 max. | 0.2 max. | 0.2 max. | 0.05 |
| 40-E* | 0.5-0.65 | 5.0-6.0 | 0.3 max. | 0.4-0.6 | 0.1-0.3 | 0.3 max. | 0.2 max. | 0.05 |
| Tenzalloy** | 0.4 | 8.0 | | | | 0.8 | | |
| Metals Hand | book, 1949, p. | 80. | | | | | | |
| **G. Caminade | e, Fonderie, 32 | 2, August, 1948 | 8, pp. 1284-5. | | | | | |

Typical Properties Ternalloy 5 Sand Cast Permanent Mold Cast Tensile Yield Elonga-Hardness Tensile Yield Elonga-Hardness Heat Strength, Strength, tion, Brinnel. Strength, Strength, tion, Brinnel. 0.2 % 500 kg load 0.2 % 500 kg load Treat psi psi 29,000 13,000 12.0 33,000 15,000 22.0 As-Cast 50 55 After 30 days at room temperature 35,000 20,000 8.5 65 43,000 22,000 16.0 70 T-51 30,000 18,000 7.0 60 36.000 21,000 16.0 70 Ternalloy 7 As-Cast 30 000 19,000 5.0 65 42,000 24.000 14.0 75 T-6 44,000 40,000 1.5 80 49,000 36,000 9.5 95 T-51 38,000 32,000 1.5 80 50,000 44.000 3.0 100 T-7 43,000 38,000 1.5 80 53,000 43,000 6.5 95

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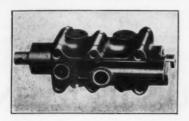


Fig. 7—Die cast in aluminum alloy 66, these castings for hydraulic pressure pump control valves must withstand 1800 psi pressure.



foundry. However, this is just a matter of good house-keeping and should not present an unsurmountable obstacle.

The alloys have been used with excellent success in this country for structural castings, machine tool parts, aircraft fittings and precision instruments where strength and dimensional stability are of prime importance. Due to their advantages, their field of application is ever increasing.

In the preceding discussion the author has tried to give a sketch of the trend of aluminum casting alloys in the United States. It has been shown that aluminum-copper alloys provide good strength alloys to the casting field; however, they lack somewhat on castability. The silicon alloys provide the good castability but lacked the strength and particularly the yield strength of the copper alloys. Combining both groups into a new one, the aluminum-copper-silicon or alumi-



Fig. 8-Microstructure of a chill cast Al-Zn-Mg type alloy (ternalloy 7). Etched, 0.5 per cent HF. 100X.

num-silicon-copper alloys, gave not only good mechanical properties but also desired foundry qualities.

The only alloy of all these groups which exhibits good ductility and impact strength is the 4 per cent copper alloy, but it had to be solution heat treated. But since it ages at room temperature it will gradually lose its high ductility and join the group of the more

common alloys. In all these alloys it is necessary to resort to heat treatment to obtain fairly good ductility.

With the development of the aluminum-zinc-magnesium alloys this has been accomplished to a great extent. Although these alloys are more difficult to cast, the foundrymen have accepted these alloys because they have learned in the last two decades to handle aluminum alloys better, and as their experience increased they had greater success with alloys which were hard to handle 20 years ago.

Conclusion

The alloys and their development discussed in the paper do not by any means constitute all of the alloys used in the United States. However, they include a large part, if not the major part, of the common casting alloys. The older alloys of aluminum-copper and aluminum-silicon alloys are still used for specific castings where one or more of the characteristic properties of the specific alloy makes its application more desirable. Also, the aluminum-copper-silicon and aluminum-silicon-copper alloys shown have many closely related derivations. These, however, do not depart in general nature from the examples.

Development of these alloys has created an ever expanding market for aluminum castings because they are easy to handle in the foundry, give excellent castings which also can be heat treated in various ways. Due to the relatively lower cost of the alloys they are more competitive with other metals. Alloys of the aluminum-zinc-magnesium group are becoming more prominent because they develop highly desirable properties even without heat treatment. It is gratifying to find that many of the alloys discussed are derived directly from scrap and are contributing so effectively to the direct progress of the foundry industry.

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A STUDY OF THE PRINCIPLES OF GATING

By

K. Grube and L. W. Eastwood*

Introduction

THE FIRST PROGRESS REPORT¹ on this subject contained a general description of the methods employed and the results obtained during the period October 1, 1947, to May 1, 1949. The present report covers the period May 1, 1949, to June 1, 1950.

The investigation has been limited to the study of pouring methods, the design of pouring box, sprue,

and horizontal runner and gate systems.

The general experimental method employed in the study includes the use of Lucite which is machined and polished to produce a high degree of transparency. The entire assembly, including the pouring box, sprue, runner and gating system, and mold cavity are contained in or made of the transparent material. Water is used for pouring because it is convenient, and transparent, and the kinematic viscosity, i.e., the viscosity divided by the density, is of the same order of magnitude as that of aluminum and magnesium melts. On the other hand, water has the disadvantage of having a surface-tension value only about one-tenth of that of the apparent surface tension of light-alloy melts.

Wood's metal was also used in the transparent molds. Although this material is opaque and, therefore, limited the observations of gas entrainment, its use aided in establishing the applicability of the transparent mold technique to the development of gating methods for the foundry.

Using the transparent mold and transparent liquid, or Wood's metal, the effects of pouring method and gating geometry are recorded photographically at 16 to 1000 frames per second.

A considerable variation in the gating geometry was obtained through the use of a number of sprues of different sizes and shapes. Also, plastic clay and Lucite inserts were employed in the runner and gates to obtain different designs. Vent holes were drilled through the Lucite mold at various points of interest in order that the aspiration of gas into the liquid in the flow channel could be studied at these locations. These techniques were used in the work reported here as well as that previously described.

The results contained in the first progress report¹

may be summarized as follows:

1. The occurrence of faulty pouring or gating practice is accompanied by the entrainment of air bubbles in the liquid stream. The degree of damage is indicated by the volume of air bubbles, and they qualitatively, though probably not quantitatively, represent the formation of dross in light-alloy melts.

2. A pouring and gating practice should be such that the flow channel and gating system are quickly filled and kept completely full during the entire pour. To accomplish this, the sprue should be tapered or otherwise restricted at the bottom so that the channel formed by the sprue can be kept filled. Abrupt changes in cross-sectional area or in direction of flow in the runner and gate system should also be avoided.

The present paper, a supplement to the motionpicture film³ in color exhibited at the 1950 A.F.S. Convention in Cleveland, Ohio, is essentially a pictorial account of the continuation of these studies, with the primary emphasis on the design of the sprue base and the development of runner and gating system consistent with the conclusions drawn in the first progress report.

Design of the Sprue Base

At the beginning of the work described in this paper, the principle appeared to be well established that a good gating system would be one that could be filled quickly and kept completely filled during the entire pour. If the gating system met this requirement, the aspiration of mold gases and the entrapment of air pockets in the liquid in the flow channel would all be avoided. While this principle is easily applied to the design of the runner and gating systems, as will be de-

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Research Engineer and Supervisor, respectively, Battelle Memorial Institute, Columbus, Ohio.

This is the second progress report on a research project conducted at Battelle Memorial Institute, initiated and sponsored by the Aluminum and Magnesium Division of the A.F.S. with some financial assistance of Battelle Memorial Institute. The project was under the general guidance of the Research Committee, of the Aluminum and Magnesium Division, which consisted of the following members: R. F. Thomson, Chairman; W. Bonsack, Co-Chairman; W. J. Klayer, C. E. Nelson, T. D. Stay, and W. E. Sicha.

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scribed later, the main difficulty arises in the design of the sprue base, or, more specifically, the design of the runner under the sprue. Although this problem of the sprue-base design may be dealt with in terms of hydraulics and laws of motion, the crux of the difficulty can be easily visualized.

Velocity of Metal Flow

When the liquid enters the sprue it falls, under the force of gravity, and gains speed so that the liquid in the flow channel has its highest velocity in feet per second as it emerges from the bottom of the sprue. The problem is to reduce this speed in the runner and gates so that the liquid will enter the mold cavity at lower velocity than it has when it emerges from the bottom of the sprue. Results of recent work carried on by others2 also indicate that some provision must be made at the base of the sprue to minimize the turbulence and to reduce the velocity of the stream entering the mold. It has been shown1 that difficulties with air aspiration, as would be expected, are more pronounced with high velocities of flow. Furthermore, unless the velocity of the liquid in the flow channel is retarded, it will enter the mold cavity at a high speed and the jet effect will cause turbulence where the liquid strikes mold walls, cores, etc., forming dross in the mold even though the metal might be clean as it enters the gate.

A fairly gentle, smooth flow from the runner through the gate into the mold cavity is mandatory when casting metals, particularly those subject to the ready formation of dross. Obviously, then, the most difficult problem in the designing of the gating system is the attainment of a reduction in the velocity of the liquid in the gating system during its travel from the sprue bottom to the gates. So far as is known, this must be done without abrupt changes in the direction or in crossing sectional area of the runner and gate system. These precautions are necessary to avoid gas aspiration into the flowing liquid or entrapment of gas pockets which require a considerable length of time to wash out of the gating system. If this time is excessively long, metal damaged by the entrainment of gas bubbles may enter the mold cavity and cause defective castings. This reduction in velocity should also be accomplished without a material reduction in the capacity of the gating system, because the problem cannot be solved by inducing difficulties with misruns and high pouring temperatures.

Resistance to Metal Flow

Because of some friction or resistance to flow in the runner and gates, some reduction in velocity and a corresponding gradual increase in the cross-sectional area of this part of the flow channel is or may be permissible. However, the greatest opportunity for dissipation of the kinetic energy, i.e., a reduction in velocity of the liquid flow emerging from the bottom of the sprue, lies in the design of the sprue base.

The design of the sprue base is a problem which does not appear to be easily solved by mathematical treatment, although such theoretical studies may be of assistance in understanding the reasons for the success of a given geometry once it has been established

by empirical methods.

In the first progress report, two different Lucite molds, A and B, were described in detail. A third and somewhat larger mold, which also has a horizontal runner and gate system for a casting having relatively little vertical height, was made and is referred to as Mold C. Using Molds B and C, both of which are illustrated in this paper, a comprehensive study of sprue-base design was made.

The problem of designing the sprue base may be minimized markedly by reducing the capacity of the gating system. This is not usually a practical solution because in foundry operations slow pouring rates mean excessively high pouring temperatures or misruns, or both. Instead, a solution was sought by studying the effects of the geometry of the sprue base. This was done first in a preliminary way by using modeling clay to effect simple changes in design. It was quickly found that, in the design of the sprue base, one or more difficulties were encountered as follows:

1. The capacity of the gating system was seriously

reduced.

2. The gating system in the region of the sprue base aspirated air, namely, sucked in mold gases.

3. Gas pockets were entrapped at the base of the sprue, and a considerable length of time was required

to wash them from the gating system.

It was indicated previously that a solution was sought that would not seriously reduce the capacity of the gating system. It was also mandatory that the geometry of the gating design completely eliminate aspiration. On the other hand, a finite time is always required to wash the entrapped gas from the gating system. The aim here, then, was to reduce this time to a minimum.

Although many different sprue bases were tried, the accompanying photographs, Fig. 1 to 18, illustrate typical effects of various sprue-base designs on the capacity, aspiration, and mold-gas entrapment in the liquid in the flow channel. The captions with these figures are self-explanatory. The photographs of the action pictures are typical of those illustrated by the motion picture, but they were taken by means of a Kenny Speedlight, using a Speed-Graphic camera and panchromatic film. Exposures of 1/6000 of a second were made after the lapse of one or more time intervals, measured from the beginning of the pour.

It is concluded from these studies that an enlargement of the runner eliminates aspiration, but causes the entrapment of mold gas under the sprue. The use of a triangular piece under the sprue of Mold B or streamlined inserts on either side of the sprue of either Molds B or C appears to be beneficial.

Design of the Gates and Runners

With the exception of that portion of the runner which is under or near the sprue, the design of the runner and gates can readily be made according to the principle that abrupt changes in direction and cross-sectional area of the flow channel should be avoided. In addition to the evidence noted in the first progress report, Fig. 19 to 31, inclusive, show examples illustrating the validity of the principle referred to previously. The captions to these figures are complete.

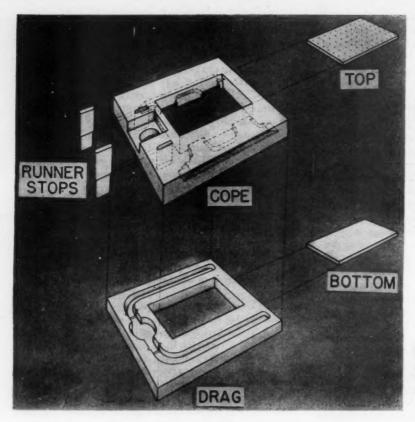


Fig. 1 (Left)—Sketch of Mold B. Runner is ¾ in. deep by 1 in. wide and is in the drag. Gates are ¾ in. by 2 in. and are in the cope. Ten different sprues were made to fit this mold. However, the one used in all the work on Mold B, illustrated by following photographs, was round, tapered, and ¾ sq in. at bottom, 2¼ sq in. at top. Consequently, ratio of areas of sprue bottom, runner, and gates is 1:2:4. Various sprue base designs were interchangeable as illustrated by Fig. 2, 4, 6 and 8.

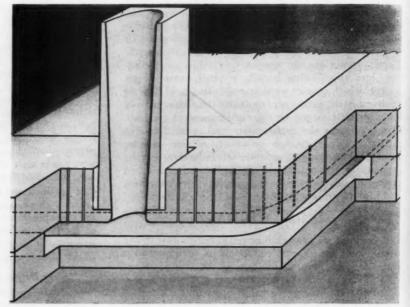
Fig. 2 (Below)—Plain inverted "T" sprue base for Mold B. The runner and tapered sprue connects to form a plain inverted "T." Dimensions are as stated in the caption of Fig. 1.

Effects of Sprue Size

Three different sprue sizes, Fig. 32, 33, and 34, were constructed for use with Mold C, which is represented by Fig. 10, 28, and 29. These three sprues, when not attached to the mold, delivered 3.44, 2.43, and 1.56 lb of water per second when the height of liquid in the pouring box was 2 in. The effects of the differences in sprue size are illustrated by Fig. 35a, b, and c, 36a, b, and c, and 37a, b, and c. The captions accompanying these figures are also complete.

It should be noted, however, that the streamlined inserts used in the runner on either side of the sprue of Mold C were designed specifically for use with the sprue, Fig. 33, which has a 3/4-in. area at the bottom. It is apparent from Fig. 35,

36, and 37 that these inserts are most effective when used with the 3/4-in. sprue. Likewise, similar inserts were less effective when used on Mold B, as represented by Fig. 8. It is concluded, therefore, that the use of these streamlined inserts placed in the runner on either side of the sprue base does not constitute a



general solution, but instead it is a specific solution for the particular geometry of the Mold C, using the sprue, Fig. 33, with the 3/4-in. cross-sectional area at the bottom. Further studies of the sprue base are, therefore, required to obtain a sprue-base design generally applicable to gating systems.

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Fig. 3 (Right)—Mold B in action, using sprue base design shown by Fig. 2. Photo was taken 1 sec after pour was started by lifting plug at top of sprue. Note severe aspiration through vertical vent holes through cope to top of runner. With mold bottom out, this gating system delivered 2.18 lb of water per sec of which 18% entered first pair of gates and 82% the second pair of gates furthermost from sprue. Without mold attached, sprue delivered 2.33 lb of water per sec, when water level in pouring box was at 2 in. of head. Consequently, friction of runner and gate system has reduced flow rate by only 7%. This is a poor gating design.

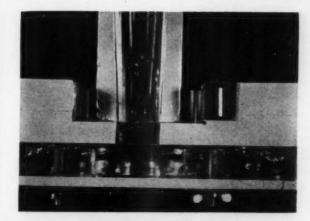
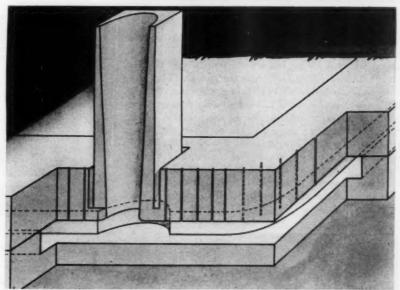
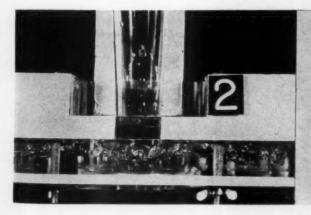
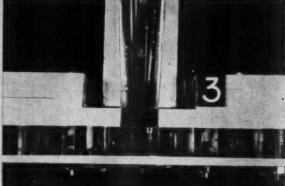


Fig. 4 (Right)—Sprue base for Mold B is similar to one shown by Fig. 2, excepting runner is enlarged to 2½ in. in diam, directly beneath sprue. Otherwise, dimensions are same as those in caption of Fig. 1.

Fig. 5A (Below, left) and Fig. 5B (Right)—Sprue base illustrated by Fig. 4 shown in action 1 and 8 sec, respectively, after pour was started by lifting plug at top of sprue. Note entrapped air bubbles at base of sprue after 1 sec, but that they are washed away after 8 sec. Enlargement has eliminated aspiration but the clean up time at base of sprue is too long. This gating system delivered 1.78 lb of water per sec, 34% of which entered the first two gates and 66% the pair of gates furthermost from sprue. Enlargement has reduced capacity of gating system by 18%.







Castings

An important part of the work described in this paper included the production of aluminum-10 per cent magnesium alloy castings in the Battelle experimental foundry. Several series of castings were made, employing gating methods similar to those used in the Lucite molds. This permits a direct correlation of the

observation made by the use of Lucite and water with the quality of the metal castings made by similar gating methods.

Supplementary to these studies of aluminum castings, a comparison was made between the production of "castings" of water and Wood's metal in Lucite molds. Figure 38 shows the water castings being made

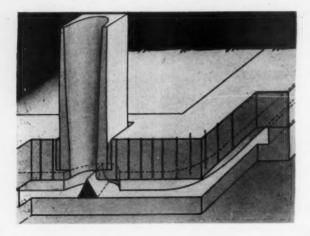
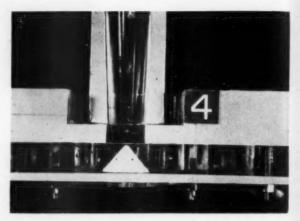


Fig. 6 (Left)—Sprue base design for Mold B, showing triangular piece ¾ in. high and 1¼ in. at base, 2½ in. long, placed across runner enlargement beneath sprue, at right angles to runner. Dimensions of sprue, runner, and gate are same as shown by Fig. 1. With exception of triangular piece, enlargement at base is same as shown by Fig. 4.

Fig. 7 (Below)—Photo shows sprue base design, illustrated by Fig. 6, in action 1 sec after pour was started by raising plug in top of sprue. Note that, because of triangular piece, entrapped air at base of sprue has been eliminated in 1 sec. Furthermore, this sprue base does not aspirate air, and therefore, constitutes a considerable improvement in design. Capacity of this sprue base and distribution of flow are almost identical to that illustrated by Fig. 4, 5A and 5B.

in Mold A by using an unsatisfactory pouring technique. This was done by pouring directly down the sprue. Mold A gating has several poor design features and is illustrated by Fig. 42 in this paper. The large volume of air bubbles in the liquid, shown on Fig. 38, indicates that the metal has been badly damaged as a result of the poor pouring and gating practice.

Figure 39 shows an enlargement from the movie taken at 1000 frames per second. The liquid is Wood's metal and the view is the top of the runner near the base of the sprue. The large volume of air bubbles escaping from the surface of the Wood's metals shows that this metal aspirates and entrains air in a manner



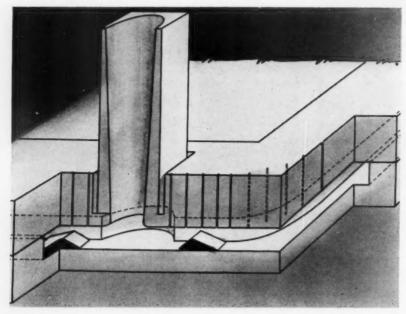


Fig. 8 (Left)—Sprue base design for Mold B showing streamlined inserts placed in runner on both sides of enlargement at sprue base. Dimensions of sprue, runners, and gates are same as shown by Fig. 1, and enlargement is same as that shown in Fig. 4. Streamlined inserts are 1 in. wide and, therefore, extend all the way across runner and are 36 in. high at highest point and 1½ in. long.

similar to that of water. Comparing Fig. 38 and 39, however, it is quite apparent that only the gas escaping from the surface of Wood's metal is visible, whereas it is visible in the body of the liquid if the latter is transparent, as illustrated by Fig. 38. This similarity

in the entrainment of air in water and in Wood's metal offers some proof of the validity of the observations made while using the Lucite-and-water, highspeed movie technique.

The castings of aluminum-10 per cent magnesium

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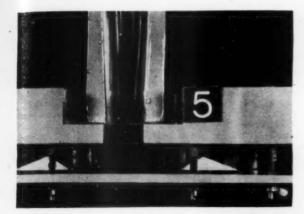
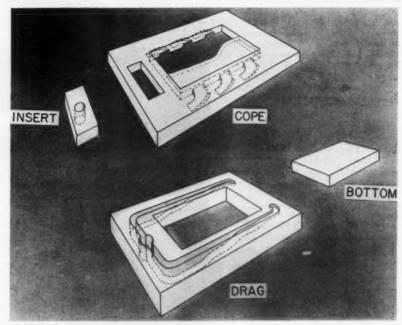


Fig. 9 (Left)—Photo shows sprue base design, illustrated by Fig. 8, in action 1 sec after pour was started by raising plug in top of sprue. Note streamlined inserts placed on both sides of enlargement have eliminated almost all air bubbles entrapped at base of sprue in 1 sec. This sprue base tends to aspirate air slightly at gate side of streamlined inserts and, though it constitutes an improved design over that illustrated by Fig. 2, it is still not entirely satisfactory. Capacity of this gating system is 1.58 lb of water per sec and about 30% enters first pair of gates and 70% through the two pair of gates furthermost from sprue. Consequently, streamlined inserts have reduced capacity of gating system about 20% as compared with design illustrated by Fig. 4 and 6.

Fig. 10 (Right)—Design of Mold C. Three sprues, illustrated by Fig. 32, 33 and 34, fit this mold. Runners at end of mold between sprue and first gate are 2 in. deep by ¾ in. wide. Each of the six gates is ¼ in. deep by 2 in. wide, having total area of ½ sq in. Consequently, cross-sectional area of runner is reduced by this amount as each gate is passed. Accordingly, runner between first and second gates is 1½ in. deep by ¾ in. wide, and between second and third gates 2/3 in. deep by ¾ in. wide. Runners are in the drag, gates are in the cope. Interchangeable sprue bases for this mold are illustrated by Fig. 11, 13, 15 and 17.



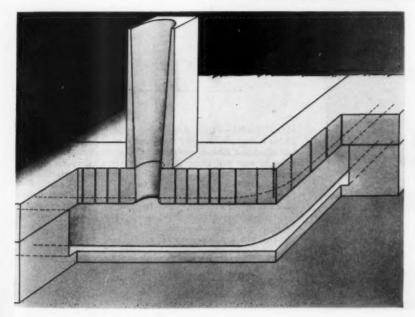


Fig. 11 (Left)—Plain inverted "T" sprue base design for Mold C. Sprue is 34 sq in. at bottom, 214 sq in. at top, and 714 in. high. Runner and gates are described in caption of Fig. 10.

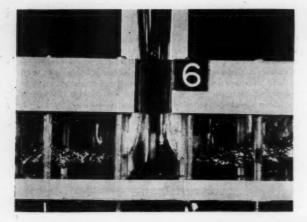
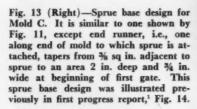
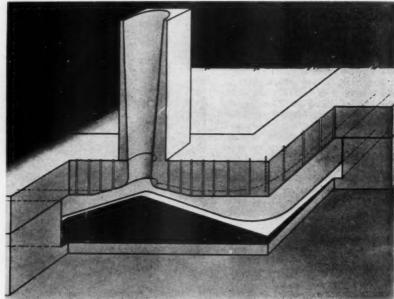


Fig. 12 (Left)—Photo of sprue base illustrated by Fig. 11 in action 1 sec after pour was started by raising plug at top of sprue. This sprue base design, like similar one shown in Fig. 2 and 3, for Mold B, aspirates air markedly through vertical vent holes in cope. Aspiration of air continues indefinitely. This gating system has a capacity of 1.89 lb of water per sec of which 40% enters first pair of gates, 31% middle pair of gates and 29% last pair of gates furthermost from sprue. Because capacity of sprue without mold attached was 2.43 lb of water per sec, resistance of runner and gates has reduced flow by 20.5%.





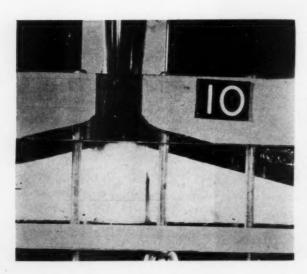


Fig. 14 (Left)—Photo of sprue base design, illustrated by Fig. 13, in action 1½ sec after pour was started by raising plug at top of sprue. Severe aspiration of air through vertical vent holes in cope and at parting line is illustrated by air bubbles highlighted in upper part of runner. This gating system has a capacity of 2.21 lb of water per sec of which 40% enters first pair of gates, 32% enters middle pair of gates, and 28% enters last pair of gates furthermost from sprue. Changing design from plain inverted "T" illustrated by Fig. 11 to this one has increased capacity of gating by 16%. However, because of severe aspiration, it constitutes a very poor design.

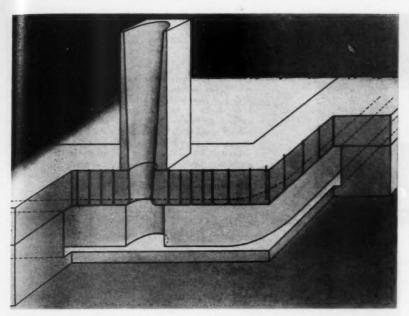
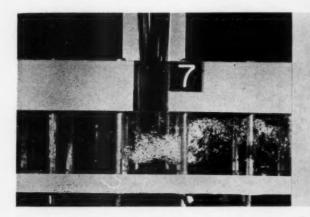
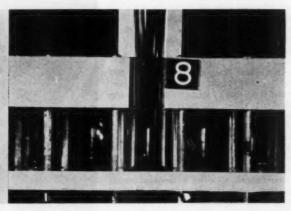
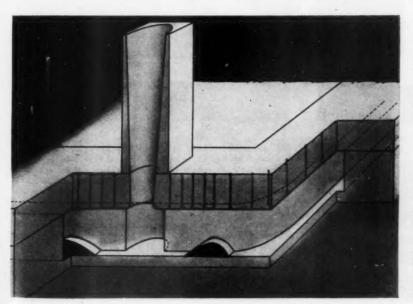


Fig. 15 (Left)—An enlarged sprue base for Mold C. Dimensions of sprue runners and gates are same as those described for Fig. 10, excepting that runner under sprue has been enlarged to 1¾ in. in diam and 2 in. deep, namely to full depth of runner. This design for Mold C corresponds to sprue base design, Fig. 4, for Mold B.

Fig. 16A (Left) and 16B (Right)—Photos of sprue base design illustrated by Fig. 15 at 1½ sec and 8 sec, respectively, after pour was started by raising plug at top of sprue. Again, the simple enlargement of runner under sprue eliminates aspiration, but a large volume of air is entrapped at sprue base, as shown by Fig. 16A. It is completely eliminated after 8 sec, as shown by Fig. 16B. This obviously constitutes a poor design be-







cause too long a time is required to wash out gas entrapped under sprue. Capacity of this gating system is 1.58 lb of water per sec of which 38% entered first pair of gates, 32% center pair of gates, and 30% through last pair of gates furthermost from sprue. Consequently, enlargement of runner under sprue has decreased capacity of gating system by 31% as compared with capacity of design illustrated by Fig. 11.

Fig. 17 (Left)—Sprue base design for Mold C with streamlined inserts on both sides of enlargement in runner under sprue. Dimensions of sprue, runners, and gates are same as those described in Fig. 10, but enlargement of runner under sprue, is 1¾ in. in diam and 2 in. deep, and is same as that illustrated by Fig. 15. Streamlined inserts placed in bottom of runner are ¾ in. wide, namely, full width of runner, ¾ in. at their highest point, and 3 in. long.

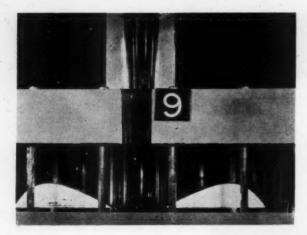
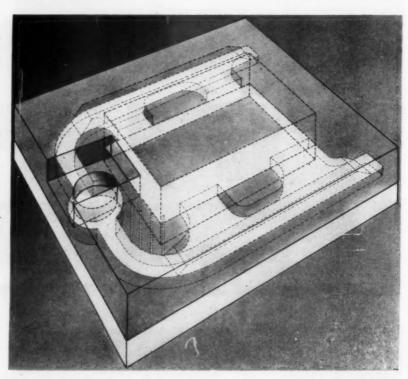


Fig. 19 (Below)—Drawing of Mold B which was illustrated in greater detail in Fig. 1. Cross-sectional area of bottom of sprue is ¾ sq in. Total cross-sectional area of the two parts of runner on either side of sprue is 1.5 sq in. and total area of the four gates is 3 sq in. This provides a ratio of 1:2:4. Note that this produces an abrupt increase in cross-sectional area of flow channel at gates. Runner is in the drag, gates are in the cope.

Fig. 18 (Above)—Photo of sprue base design, illustrated by Fig. 17, in action 11/2 sec after starting pour by raising plug at top of sprue. As shown by photo, sprue base cleans up in 11/2 sec or less. As compared with design illustrated in Fig. 15, 16A and 16B, inserts have greatly reduced time required to eliminate en-trapped air under sprue. Furthermore, this design does not aspirate air. Capacity of gating system illustrated is 1.63 lb of water per sec of which 38% entered first gate, 32% center pair, and 30% entered third pair of gates furthermost from sprue. Capacity of this gating design is about 3% greater than that of same design without inserts illustrated by Fig. 15, 16A and 16B. Apparently, a slightly more uniform distribution of liquid to the three pairs of gates could be obtained by gradually increasing crosssectional area of flow channel beyond first gate instead of using a constant crosssectional area of flow channel. This increase might be perhaps 15% at last gate. This not only would produce still more uniform distribution of flow through the three pairs of gates, but linear speed of flow through the three pairs of gates would also be reduced.



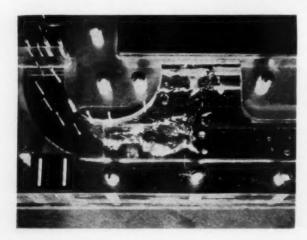
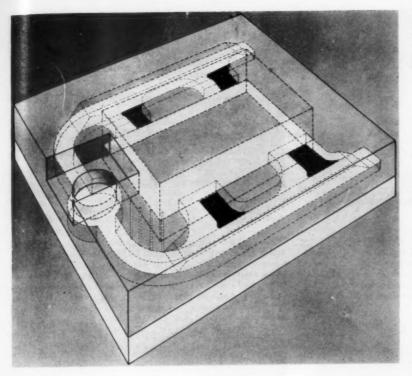
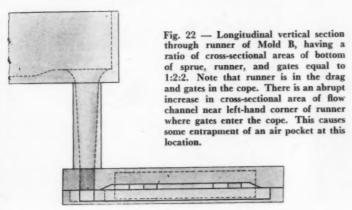


Fig. 20 (Left)—Photo of runner and first gate in Mold B, illustrated by Fig. 19, in action. Vertical vent holes extending through cope to runner are clearly evident. Note separation of liquid in gate because of abrupt twofold increase in cross-sectional are of flow channel at this point. Obviously this constitutes a poor runner and gate design. Separation of liquid in gate continues indefinitely through portions of gas pocket are gradually washed into mold cavity where they could cause casting defects.





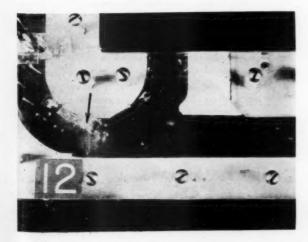


Fig. 21 (Left)—Drawing of Mold B similar to that shown by Fig. 19, with exception that cross-sectional area of gates has been reduced by one-half, producing a ratio of cross-sectional areas of sprue bottom, runners, and gates equal to 1:2:2.

alloy are 8 by 12 by 1/4-in. thick. The gating designs are illustrated by Fig. 41, 42, 43, and 44. Figure 45 shows a gating design used commercially and will be investigated in the future. The gating systems illustrated by Fig. 43 and 44 were used with the three different sprue sizes represented by Fig. 40. The cope surface and radiographic quality of representative castings made by the three principal gating systems are illustrated by Fig. 46 to 51, inclusive. Using these figures as standard ratings, the quality of the cope surfaces and the radiographic appearance of the other castings made are listed in Table 1. This phase of the work has not been completed but the preliminary results clearly indicate the fol-

1. The method of gating illustrated by Fig. 41 usually produces quite unsound castings; those produced by the gating illustrated by Fig. 42 are better, while those produced by the gating illustrated by Fig. 43 and 44 are much the soundest. Thus, the results of the studies with Lucite molds are confirmed, but each design feature has not been evaluated by making castings in the foundry.

Fig. 23 (Left)-Photo of action in Mold B of design illustrated by Fig. 21 and 22. Photo was taken 11/2 sec after pour was started by raising plug at top of sprue. Because abrupt increase in cross-sectional area of flow channel at gates has been eliminated, no separation occurs in gate. However, a small air pocket occurs at point of abrupt increase in crosssectional area where gates enter the cope. This is indicated by arrow. Obviously such abrupt increases in cross-sectional area in flow channel should be avoided because, if such air pockets are washed out, they could cause casting defects. Capacity of this gating system is at least equal to or possibly slightly greater than capacity of gating system illustrated by Fig. 19 and 20, thus indicating that changing ratio from 1:2:4 to 1:2:2 has not appreciably affected flow rate. The 1:2:2 ratio, however, is not entirely satisfactory because linear velocity of flow at sprue base has been reduced only one-half. Consequently, linear velocity of flow as liquid enters mold cavity may be too high. A 1:4:4 or perhaps a 1:6:6 ratio would be better.

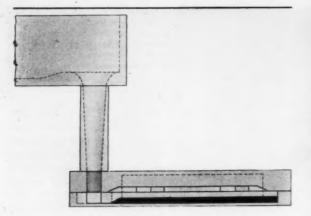


Fig. 24—Longitudinal vertical section through runner of Mold B. An insert has been placed in bottom of runner to compensate for increase in cross-sectional area of flow channel where gates enter the cope. Area of sprue, runner, and gates is otherwise similar to that described in caption of Fig. 19.

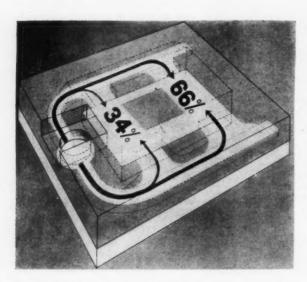


Fig. 26—This line drawing shows uneven distribution of flow in Mold B, having 1:2:4 ratio. Dimensions of sprue, runner, and gates are same as those illustrated by Fig. 19. Because of the inertia of the liquid, a large portion of it flows through the last pair of gates furthermost from the sprue.

2. In general, the smaller sprues represented by Fig. 40 produced the soundest castings. The tapered sprue having only 5/16 sq-in. in area at the bottom did not cause misruns at pouring temperatures of 1300 F, nor did it reduce the pouring time appreciably.

Summary and Conclusions

Using the transparent-water, transparent-mold, highspeed photograph technique for studying gating, further investigations have been made of the design of the sprue base and the horizontal runner and gate systems. To obtain a correlation between the results

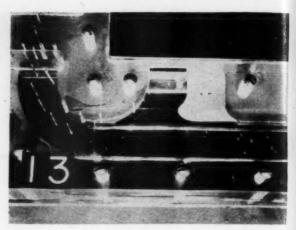


Fig. 25—Photo of gating system, illustrated by Fig. 24, in action 1½ sec after starting pour by lifting plug at top of sprue. Comparing this with photo in Fig. 23, it is quite evident that elimination of abrupt increase in cross-sectional area where gates enter cope has eliminated air pocket at location of arrow in Fig. 23. Capacity of this gating system, surprisingly enough, is 3.5 per cent greater than that of gating system without insert in runner, illustrated in Fig. 19 and 21.

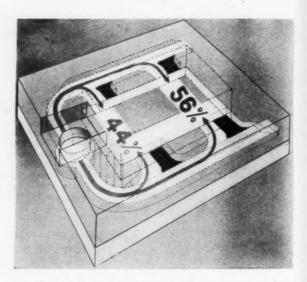


Fig. 27—This drawing shows flow distribution in Mold B when cross-sectional area of gates has been reduced by one-half to produce a ratio of cross-sectional area of bottom of sprue, runners, and gates equal to 1:2:2. A more uniform flow distribution is obtained. However, design illustrated by Mold C in Fig. 28 is preferred for reasons outlined in caption of Fig. 23.

of this study and casting quality, foundry tests were made by pouring castings with various gating designs. On the basis of this work and that reported previously, the following conclusions may be drawn regarding the design of the sprue base and the remainder of the horizontal runner and gating system.

General Principles

The pouring technique and gating design should be such that the gating system can be completely and quickly filled before any liquid enters the mold cavity, th

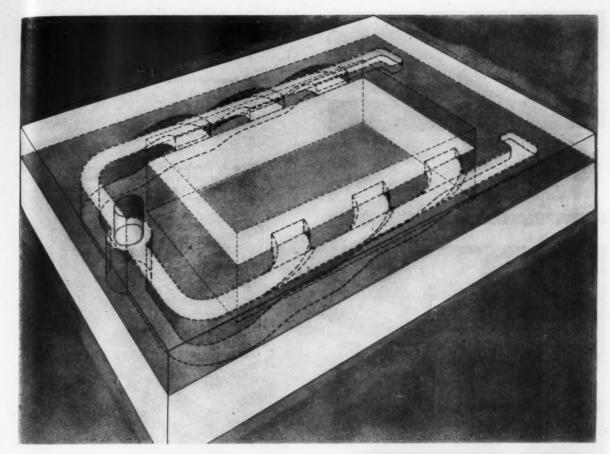


Fig. 28—Simplified drawing of Mold C shown in greater detail by Fig. 10. The three sprues illustrated by Fig. 32, 33 and 34, fit this mold. The runner is in the drag and is 2 in. deep and ¾ in. wide, but as each gate is passed, the cross-sectional area of the runner is reduced by the area of the gate, thereby equalizing the flow through the gates. Since cross-sectional area of the gates is ½ sq in., depth of the runner is reduced by 2/3 in. at each gate. It will be noted that the ratio of cross-sectional area of the bottom of the sprue, runner, and gates in this instance is 1:4:4 when the area of the sprue is ¾ sq in. as illustrated by Fig. 33.

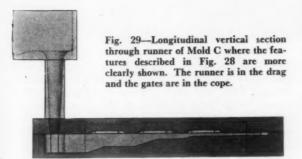


Fig. 30—Photo of Mold C in action 1 sec after starting pour by raising plug in top of sprue. The first gates, namely the one nearer the sprue, is illustrated. Note absence of separation in the gate. There are some bubbles in the flow stream which are remanents of air entrapped at base of sprue as illustrated in previous photos. About 1¼ sec are required to wash out trapped gas at base of sprue of this design.

and that it can be kept full during the entire pour. If the flow channel can be filled quickly and kept full, gas entrainment from the vortex, aspiration, and gas entrapped during the initial part of the pour can be avoided or minimized. To make use of this general principle, it is necessary to avoid abrupt changes in direction of flow or in cross-sectional area of the flow channel from the top of the sprue to the mold cavity.

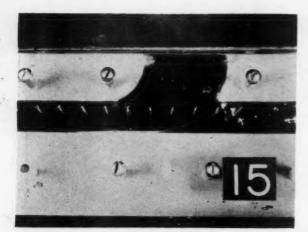
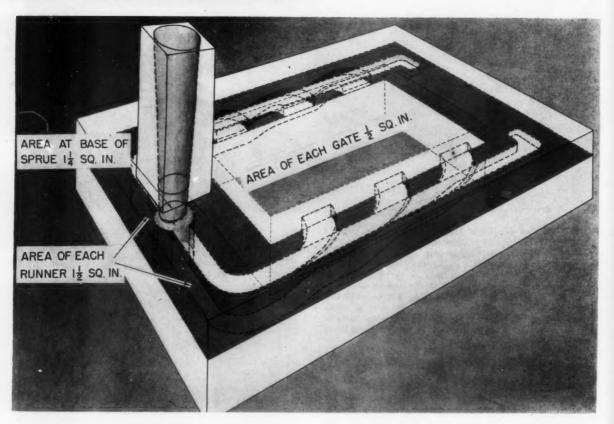


Fig. 31—This photo is similar to Fig. 30, but the third gate, namely the one furthermost from sprue is illustrated. The photo was taken 1 sec after start of pour. Note here, also, absence of separation in gate and single air bubble in flow stream. This photo and Fig. 30 shows that the most damaged liquid enters the first gate. This observation has been confirmed by the study of aluminum castings made with similar gating system in the foundry.

Fig. 32—Drawing showing one of three sprue sizes which fit Mold C. Because of the 1¼ sq in. area at bottom of sprue, ratio of this cross-sectional area to total cross-sectional area of runners and gates of Mold C is 1:2.4:2.4. Sprue height is 7¼ in.



The one location where abrupt changes in direction and cross-sectional area are necessary and desirable is the sprue base or that portion of the runner under and adjacent to the sprue.

Design of the Sprue Base

The liquid poured into the mold has its highest velocity as it emerges from the bottom of the sprue. The principal problem of gating involves the reduction of this velocity of flow in the flow channel between the sprue bottom and the mold cavity so that the metal does not enter the latter too violently. The kinetic energy of the liquid emerging from the bottom of the sprue must be partially dissipated at the sprue

base. A 4-to-1, or 6-to-1 reduction in velocity can be effected by making an enlargement in the runner under the sprue and placing streamlined pieces in the runner which partially restrict it on either side of the enlargement. This was only completely satisfactory for one combination of sprue and runner dimensions. The streamlined pieces of fixed dimensions, forming the restrictions on either side of the enlargement, were slightly less successful with other sprue and runner geometry.

A triangular piece placed at right angles to the runner immediately below the sprue and all the way across the enlargement was very successful for one specific set of sprue and runner dimensions only. It is

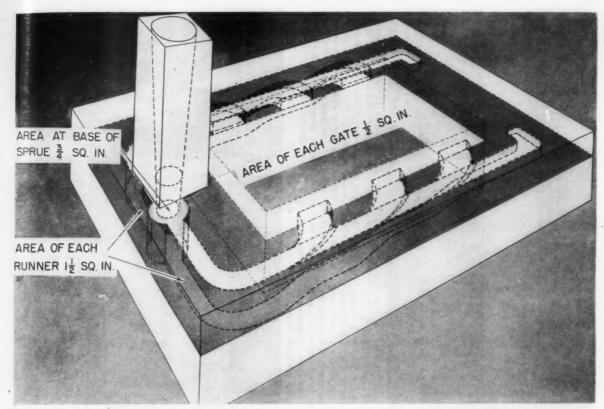
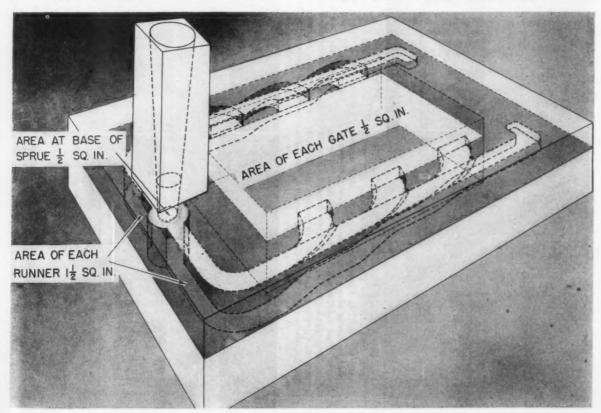


Fig. 33 (Above)—This sprue, also for Mold C, is only ¾ sq in. at bottom, producing a gating ratio of 1:4:4.

Fig. 34 (Below)—Mold C sprue $\frac{1}{2}$ sq in. in bottom area providing gating ratio of sprue, runner, and gate area of 1:6:6.



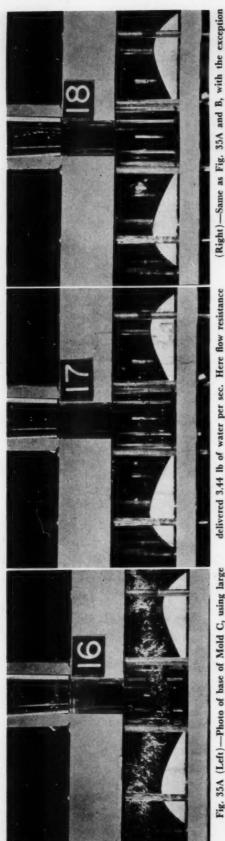


Fig. 35A (Left)—Photo of base of Mold C, using large sprue represented by Fig. 32 which produces a gating ratio of 1:2.4:2.4. Photo was taken 1 sec after starting pour by raising plug at top of sprue. Note considerable mass of air bubbles which remain at bottom of sprue despite streamlined pieces which were found successful with the ¾ sq in. Capacity of this gating system is 1.93 lb of water per sec. Without mold attached, this sprue

delivered 3.44 lb of water per sec. Here flow resistance in runners and gates has reduced capacity by 45%. This should be compared with data for the smaller spiues represented by the following figures. Fig. 35B (Center)—Same as Fig. 35A, but photo was taken 3 sec after start of pour. Note that a few bubbles remain at base of sprue which have not yet washed out. It will be shown in subsequent photos that this part of flow system is cleaned up more rapidly when smaller sprues are used. Fig. 35C

damaged liquid during initial part of pour. This photo

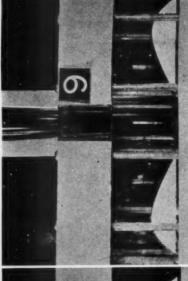
and Fig. 35A and B should be compared with Fig. 36A

B, C and Fig. 37A, B and C.

general, this is too long a time to climinate occurrence of

but about 5 sec were required to accomplish this.

that photo was taken 5 sec after start of pour. It will be noted that all of the air bubbles have been washed out.



pared with larger sprue, it is evident that occurrence of damaged liquid can be reduced appreciably by using smaller sprue. Capacity of this gating system is 1.63 lb of water per sec or only 15.5% less than obtained by a sprue having to 1 2/3 times larger cross-sectional area. This sprue delivernes 2.43 lb of water per sec when it is not attached to the mold.

Here resistance of runners and gates reduced flow 33%...

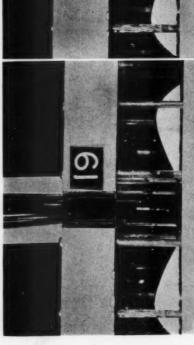


Fig. 36A (Left)—Photo of base of Mold C in action, using sprue having ¾ sq in. cross-sectional area at bottom, as illustrated by Fig. 33. This cross-sectional area provides a gating ratio of 1:4:4. Photo was taken 1 sec after pour was started by raising plug at top of sprue. Note that only a few air bubbles remain at base of sprue and these are being rapidly washed out. Fig. 36B (Center)—Same as Fig. 36A,

with exception that photo was taken 11/4 sec after start of pour. All of the air bubbles have been washed out. The inserts placed in runner at each side of enlargement were designed for this specific sprue size. Fig. 36C (Right)—Similar to Fig. 36B, with exception that photo was 11/2 sec after start of pour. Note that all of the air bubbles at hase of sprue have been washed out in 11/4 sec or less. As com-

Mence resistance of runners and gares reduced now 115

of sprue have been washed out in 1/4 sec of less. As com-

aly washed out. Fig. 30h (Center) — Same as Fig. 30A.

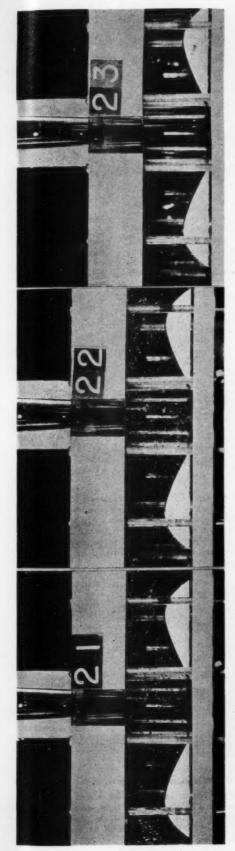


Fig. 37A (Left)—Photo shows Mold C in action when a sprue is used having a cross-sectional area at bottom of only ½ sq in. This sprue provides a gating ratio of 1:6:6 in Mold C. Photo was taken 1 sec after starting pour by raising plug at top of sprue. A considerable volume of aim remains, volume appearing to be somewhat greater than that shown in Fig. 36A despite the smaller sprue which has been used. Without mold attached this sprue delivered 1.56 Ib of water per sec and 1.37 Ib with the mold attached. Hence runners

and gates have reduced the capacity by only 12% and capacity of entire system is only 29% lower than that obtained when a sprue 2½ times larger is used. About 38% entered the first pair of gates, 32% through the second pair, and 30% through the pair furthermost from the sprue. Fig. 37B (Center)—Same as Fig. 37A, with exception that photo was taken 1¼ see after starting pour. A considerable volume of air bubbles remains at hase of sprue, whereas with the larger ¾-in, sprue, illustrated by Fig. 36B, no air

bubbles remain. Fig. 37C (Right)—Same as Fig. 37A and 37B with exception that photo was taken 1½ sec. after start of pour. Very few air bubbles remain after 1¾ sec. It is apparent that the streamlined inserts placed on either side of enlargement at base of sprue are most effective for the ¾-in. sprue, Fig. 33, providing a gating ratio of 1:4:4. Actually the streamlined inserts were designed for this particular gating system and not for the larger and smaller sprues represented by Fig. 32 and 34, respectively.

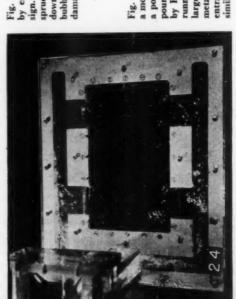
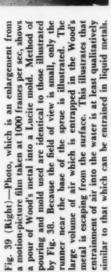
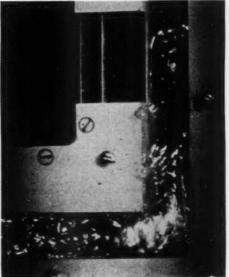


Fig. 38 (Left)—Photo of a water casting being made by employing a poor pouring technique and gating design. A large volume of air formed in the vortex of the sprue is being discharged at base of sprue where it runs down the runner and into the mold cavity. These air bubbles qualitatively, but not quantitatively, represent damaged liquid or dross in light-alloy castings.





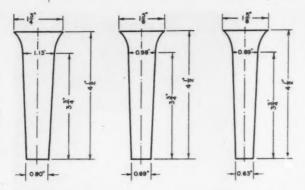


Fig. 40—Several aluminum alloy castings were made in the foundry to check the gating principles developed by use of water in Lucite molds. This figure shows three different sprue sizes employed in production of test castings. All were tapered 2:1 and their cross-sectional areas at base of sprue are 0.500, 0.375, and 0.312 sq in.

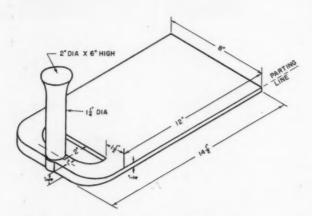


Fig. 41—This gating system for a simple plate casting 8x12x1/4 in, thick is typical of that used in some foundries. Note large untapered sprue under gates leading directly to the casting.

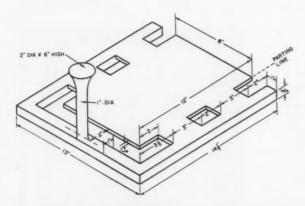


Fig. 42—Gating system illustrated here for a simple casting 8x12x1/4 in. was also investigated by using Lucite molds and water. It was found that the gating design was faulty because of abrupt changes in direction and cross-sectional area of flow channel. The untapered sprue also offers the opportunity for mold gas entrainment. The runner and gates are in the drag. The gating system is similar to that illustrated by Fig. 38, a Lucite mold described in more detail in the First Progress Report, Fig. 6 and 7.

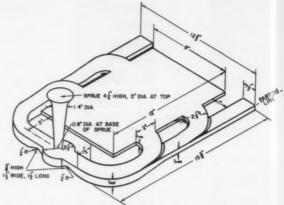


Fig. 43—This gating system for a simple casting 8x12x½ in. is similar to that of Mold C illustrated by Fig. 10 and 28. The three different sprues illustrated by Fig. 40 were employed with this gating system, thereby permitting a study of the effect of sprue size. Using the three sprues illustrated by Fig. 40, from left to right produced gating as follows: 1:4:4, 1:5.3:5.3 and 1:6.4:6.4.

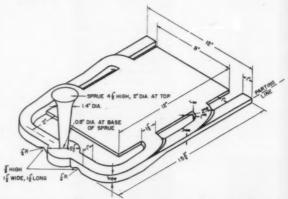


Fig. 44—That gating system for a plate casting 8x12x¼ in. is similar to that illustrated by Fig. 43, with the exception that the cross-sectional area of runner between sprue and first gate is ¾ sq in. instead of 1 sq in. The three sprues illustrated by Fig. 40 were also employed in studies to determine effect of sprue size. Using the three sprues, illustrated by Fig. 40, left to right, the gating ratios were as follows: 1:3:3, 1:4:4 and 1:4.8:4.8.

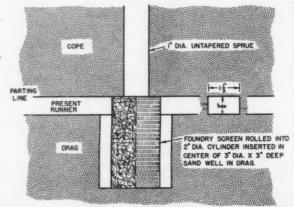


Fig 45—A gating system employing the screen follows the practice recommended by a commercial light metal foundry for a casting of this type. The remainder of the gating system is similar to that shown by Fig. 43. Castings have not yet been made with this gating design.

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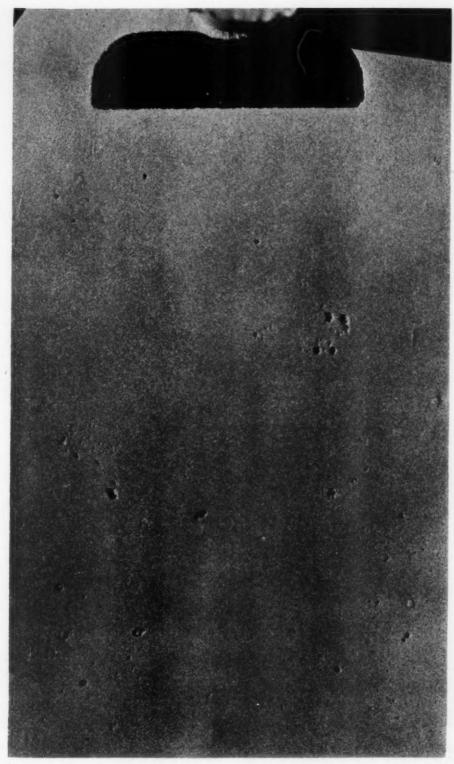


Fig. 46—Photo shows cope surface of a sandblasted casting of an aluminum alloy containing 10% magnesium. Gating employed is illustrated by Fig. 41. The extreme degree of ungation. The ratio of poured weight to casting weight is 1.5:1.

concluded that this is by no means a general solution to the problem of sprue-base design.

The design of the gates and runner, other than that near the base of the sprue, is fairly straightforward

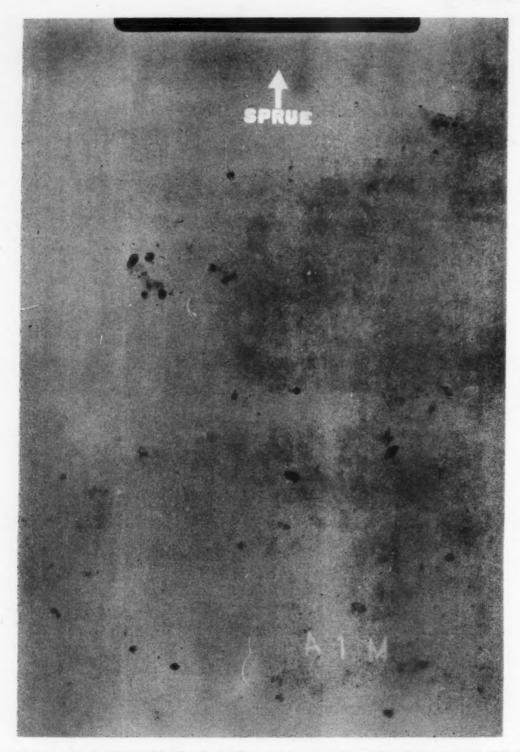


Fig. 47—Radiograph of casting illustrated by Fig. 46. It will be observed that, in addition to the severe unsoundness of cope

surface, many areas of dross, dark spots, occur within the body of the casting as well.

because abrupt changes in direction and in crosssectional area of the flow channel can be fairly readily avoided or reduced in magnitude. To avoid abrupt changes in direction of flow, the corners of the runners were rounded to as large a radius as possible, and the juncture of the gates and runner was also rounded to largest possible radius on sprue side.

To avoid abrupt changes in the cross-sectional area

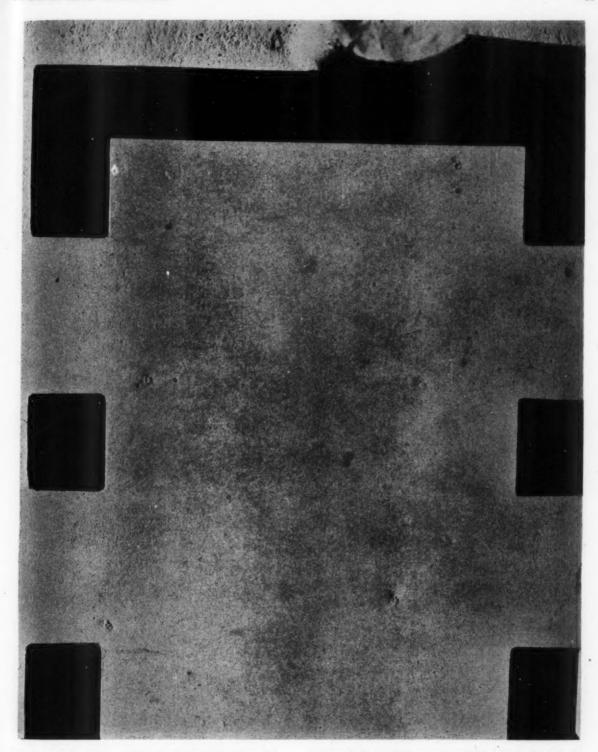


Fig. 48—Photo shows a sandblasted cope surface of a casting of aluminum-base alloy containing 10% magnesium. The gating system used is illustrated by Fig. 42. A few cope surface defects

are noted, but quality is considerably improved over that represented by Fig. 46 and 47. Ratio of poured weight to casting weight is 2.4:1.

of the horizontal part of the flow channel, the total area of the gates was the same as the total crosssectional area of the runner between the sprue base and the first gate. At each gate, the cross-sectional area of the runner was decreased by the area of the gate so that abrupt changes in cross-sectional area of

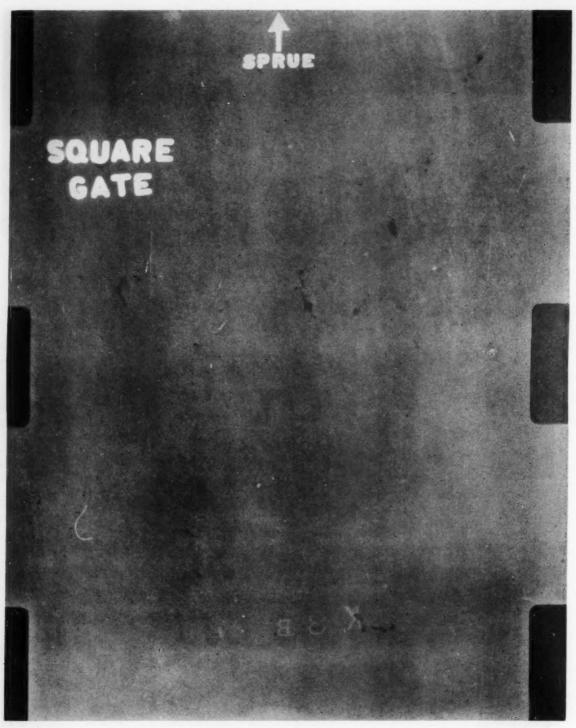


Fig. 49—An X-ray typical of castings made by gating method illustrated by Fig. 42 and Fig. 48. X-ray shows internal dross casting represented by the X-ray, Fig. 47.

defects not apparent on the surface. Again, however, it constitutes a considerable improvement over the soundness of the

the flow channel at these locations were avoided. A uniform distribution of flow through the gates was also obtained.

Although other methods may also be satisfactory, it

is concluded that, when the runner is placed in the drag and the gates in the cope, the sprue and runner can be filled before metal enters the mold cavity. This provides an opportunity for the collection of dross



Fig. 50—Photo of cope surface of a casting made by Mold C was 5/16 sq in., producing a gating ratio of 1:4.8:4.8. Cope gating design, illustrated by Fig. 44. Area of bottom of sprue surface of this casting is substantially free of dross defects. Ratio of poured weight to casting weight is 2.4:1.

along the cope side of the entire length of the runner. It also reduces the amount of damaged metal which enters the mold cavity.

A satisfactory runner and gate design for castings

having low height is illustrated by Fig. 28. It should be scaled up or down, depending upon the size of the castings. To use this gating design, the size of the bottom of the sprue should be as small as possible and

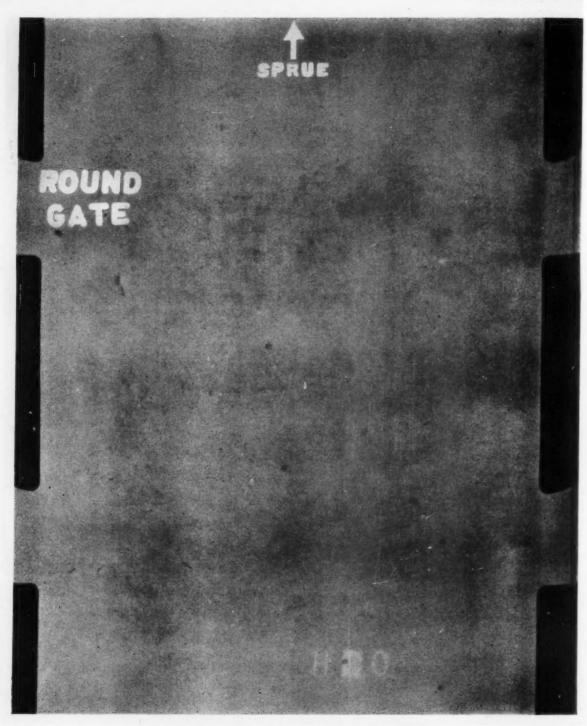


Fig. 51—Photo of X-ray film of casting represented by Fig. 50. Substantial freedom from internal dross defects is representative of many castings made by this method.

just large enough to permit pouring rates that eliminate misruns without the use of high pouring temperatures. The cross-sectional area of the runner on either side of the sprue between the sprue and first gate should be 2 to 3 times the cross-sectional area of the

bottom of the sprue. The total area of the gates should be equal to or slightly larger than the total crosssectional area of the runner between the sprue and the first gate. The size of the runner should be gradually reduced at each gate as previously indicated. The run-

Table 1-Ratings of Cope Surfaces and Radiographic Quality of Aluminum-10% Magnesium Castings*

| Heat No. | | | | | | | old C Type Sq In. Rus (Fig. 43) | nner | | | | | 3/4 Sc | | pe*** unner 4) | |
|-------------|--------------------------------|-------|-----------------------|-----------|-----------------|-----------|---------------------------------------|-----------|-----------------|-----------|-----------------|-----------|-----------------|---------------------------------------|----------------------|-----------|
| | Sprue and Gate (Fig. 41) | | Mole Type (Fig. | e** | | Ratio | 3/8 Sq In. 5 Gating R 1:5.3:5 | atio | | Ratio | | Ratio | Gating | Sq In. Sprue Gating Ratio 1:4:4 | | Ratio |
| | Cope Surface | | Cope Surface | X- Ray | Cope Surface | X- Ray | Cope Surface | X- Ray | Cope Surface | X- Ray | Cope Surface | X- Ray | Cope Surface | X- Ray | Cope Surface | X- Ray |
| 6483 | 7 | 9 | 5 | 7 | 5 | 5 | | | | | | | | | | |
| 6484 | 6 | 8 | 5 | 6 | 5 | 5 | | | | | | | | | | |
| 6485 | 7 | 9 | | | 1 | 4 | | | | - | | | | | | |
| 6486 | 6 | | 3 | 7 | 1 5 | 5 | | | | | | | | | | |
| 6508 | | | | | | | | | | | | | | | | |
| 6509 | | | | | 5 | 6 | | | | | | | | | | |
| 6509 | | | | | 4 | 2 | | | | | | | | | | |
| 6509 | | | | | 6 | 6 | | | | | | | | | | |
| 6010 | | | | | 6 | 7 | 1 | | 3 | 3 | | | | | | |
| 6510 | | | | | | | 1 | | 1 | 3 | | | | | | |
| 6511 | | | | | | | 1 | | • | | | | | | | |
| 6511 | | | | | | | 9 | | | | | | | | | |
| 6517 | | | | | | | 1 | | | | | | | | | |
| 6517 | | | | | | | 1 | | | | | | | | | |
| 6517 | | | | | | | 3 | | | | | | | | | |
| 6559 | | | | | | | | | | | | | 1 | 4 | | |
| 6559 | | | | | | | | | | | | | 1 | 5 | | |
| 6559 | | | | | | | | | | | | | 1 | 2 | | |
| 6559 | | | | | | | | | | | | | 2 | 4 | | |
| 6560 | 6 | 8 | 4 | 7 | 3 | 3 | | | | | | | | | | |
| 6561 | 6 | 7 | 2 | - 5 | 9 | 3 | | | | | | | 3 | 2 | | |
| 6562 | 6 | _ | ī | _ | 3 2 3 | | | | | | | | 1 | 3 | | |
| 6563 | - | | | | 2 | 2 | 1 | 1 | 1 | 1 | 2 | 3 | 1 | 1 | 1 | 1 |
| 6564 | 7 | 7 | | | _ | - | | | - | | | | 1 | 1 | - | |
| 6565 | | | 5 | 5 | 1 | 1 | 1 | 2 | 1 | 1 | | | | | | |
| 6566 | | | | | - | | | - | | | 1 | 2 | 2 | 3 | 1 | 1 |
| 6567 | | | | | 1 | 1 | | | | | | | 1 | 1 | | - |
| 6568 | | | | | | | | | | | | | 3 | 2 | | |
| 6581 | | | | | 1 | 9 | | | | | | | 1 | ī | | |
| * See Fi | g. 46 to 5 | | | phs o | of standar | ds of | radiograp | hic | Datino | | | Ratin | g Guide | | | |
| | ope surface | | | | ** | | | | Rating | , | 0 | 66 | _ | | v n | |
| | ig. 6 and | 7, R | et. I, for | corr | esponding | gati | ng design | in | No. | | | Surfac | | | X-Ray | |
| | molds | 00.1 | | | | | | | 1 | | | Fig. 50 | | | See Fig. | |
| | g. 10 and | 38 in | this pape | er for | correspon | ding | gating des | ign | 5 | | | Fig. 48 | | | See Fig. | |
| in Luc | rite mold. | | | | | | | | 8 | | See | Fig. 46 |) | | See Fig. | 47 |

Increasing unsoundness is indicated by increasing rating numbers.

ner on either side of the enlargement under the sprue should be slightly restricted, as illustrated by Fig. 17.

Correlation With Metal Castings

When an unsatisfactory pouring and gating technique is used, Wood's metal entraps air qualitatively similar to the entrapment of air by water poured under the same conditions. Castings of aluminum-10 per cent magnesium alloy have been made, the quality of the cope surface examined, and radiographic soundness determined. An excellent correlation was obtained between the occurrence of dross in or on the castings and the evaluation of the gating design by the transparent-mold, transparent-water, high-speed photography technique. Also in conformity with these studies, it was indicated that the sprue should be as small as possible and still avoid misruns at low pouring temperatures.

Acknowledgments

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- 1. "The Study of the Principles of Gating." by R. E. Swift, J. H. Jackson, and L. W. Eastwood, Transactions, A.F.S., vol. 57, p. 76-88 (1949).
- 2. "Flow of Metal Through Runner Systems." by Dr. F. B. Tedds, Foundry Trade Journal, vol. 88, pp. 443-452, April 27,
- 3. Motion Picture "Study of Horizontal Gating Systems" by K. Grube and L. W. Eastwood. May 8, 1950, at the A.F.S. Convention in Cleveland. Ohio.

DISCUSSION

Chairman: R. F. THOMSON, International Nickel Co., Detroit. Co-Chairman: C. E. Nelson, The Dow Chemical Co., Midland, Mich.

A. L. Kearney and C. W. Witters (Written Discussion): 1 Recent studies of gating systems to establish fundamental principles have been a major step in improving the science of founding. When these principles are established, the average quality standards of castings should be improved and the cost of producing them reduced.

The most active and comprehensive study of foundry gating is that sponsored by the Aluminum and Magnesium Division of the American Foundrymen's Society at Battelle Memorial Institute. The results of this work have been very interesting, and the men carrying on these studies are to be congratulated for their excellent treatment of the subject. The initial report on this work, entitled "A Study of the Principles of Gating," by R. E. Swift, J. H. Jackson and L. W. Eastwood, appears in A.F.S. Transactions, vol. 57 (1949).

In this report, as well as in others referred to in the appended bibliography, reference is made to studies directed toward reducing aspiration in gating systems. In no case is it felt that the harmful effects of aspiration occurring during casting have been clearly established. Tests have not yet been made which separate the effects of aspiration, metal turbulence during pouring, and other factors that are known to result in dross and oxides and entrained gas holes in castings.

Experiments reported in this discussion were designed to evaluate some of the suggestions with respect to methods of minimizing aspiration, contained in the 1949 progress report. These experiments and the conclusions that might be drawn from them are strictly limited to a gating system in a permeable medium. They are presented at this time as constructive comments on the work reported to date. The tests confirm the doubt, expressed in the initial report, that results obtained in the plastic molds could be transferred directly to permeable sand molds.

In the tests to be described, the following conditions were standard:

- Semi-synthetic green sand molds were used, the molding sand possessing an A.F.S. grain fineness number of 150 and permeability of 25–27.
- Aluminum alloy No. 356 was used (7% Si, 0.3% Mg, Bal. Al).
- The metal used was carefully fluxed with dry nitrogen and thoroughly skimmed before pouring.
- 4. All molds were poured with hand ladles of the same design.5. No risers or mold vents were used that would allow created
- gas or oxides to be washed out of the mold cavity.

 6. Other than the use of a pouring basin or box, no artificial pouring aids such as strainers or basin plugs were employed.

Test No. 1-Study of Gating Systems Proposed by Battelle.

A pattern 5 in. x 11 in. x $\frac{1}{4}$ in. was gated according to the runner and gate design suggested by Swift, Jackson and Eastwood. For practical reasons this pattern was not as large as the one used at Battelle, but it is felt that it is a reasonable substitution, as the pattern is used only as a means for analyzing the gating system and was the same for all tests herein described. Figure A shows the poured casting with the gating attached. Figure B shows the pouring box and sprue cut from one of the castings. The pouring box was 5 in. wide x 5 in. deep x 12 in. long. The reproductions of the radiographs shown in Fig. C and D indicate that considerable agitation occurred in the system as evidenced by the large amount of gas and oxides shown in both the gates and the casting.

Test Conditions: 1. Elongated pouring basin.

- 2. 1 in. x 3/4 in. x 6 in. tapered sprue with the small end down. Minimum sprue area 0.75 square inches.
- Runners and gates streamlined with the gates on the cope side of the runner, i.e. no cope runner section.

Test No. 2-Study of Gating System Using Reverse Taper Sprue.

The pattern used in Test No. 1 was gated in a manner typical of certain production gating practices. The sprue has reverse taper, the gates are at right angles to the runner and the pour was made directly down the sprue. This gating (Fig. E) should be highly aspirating as described in the paper by Swift, Jackson, and Eastwood.⁷ The reproduction of the radiographs show a sound casting, Fig. F, and gating system, Fig. G.

Test Conditions: 1. Shallow pouring basin directly over the sprue.

- 2. Reverse taper sprue 13/8 in. x 3/8 in, x 5 in. Minimum sprue area 0.52 square inches.
- 3. Runner half in the cope and half in the drag with the gates at the center line, i.e. runner both below and above gates.

Test No. 3-Study of Reverse Taper Sprue Combined with Gating System Proposed by Battelle with Recommended Runner and Gates.

As an additional check, the pattern shown in Fig. A was used with the suggested runners and gates but with an aspirating type sprue of a design similar to the one used in Test No. 2. (Fig. H.) The reproductions of typical radiographs in Fig. I and J show a relatively sound and clean casting and gating system. There is a small amount of entrained gas and oxide near the first gate on the right side of Fig. I.

Since the same design of runners and gates produced divergent results with different types of sprues, these tests would indicate that the design of the sprue, as proposed by Battelle, may promote agitation of the molten metal.

Test Conditions: 1. Shallow pouring basin directly over the sprue.

2. Reverse taper sprue 15% in. x 5% in. x 6 in. minimum sprue area 0.61 square inches.

3. Runners and gates streamlined with gates in the cope and runner in the drag.

Test No. 4-Study of Enlarged Reverse Taper Sprues.

In the previous two tests the minimum sprue area was less than the 0.75 square inches used in Test No. 1. To demonstrate that sound castings can be poured through a larger area sprue, castings gated as shown in Fig. K were poured. Two aspirating type sprues were employed and the metal was poured into a shallow basin directly over the sprues. A reproduction of a typical radiograph, Fig. L, shows a relatively sound and clean casting.

Test Conditions: 1. Shallow pouring basin directly over the

- 2. Two reverse taper sprues 15% in. x 3% in. x 6 in. Minimum sprue area 1.22 square inches (Taper approximately 5 deg per side).
- Runner half in the cope and half in the drag with gates at the center line.

SUMMARY

In these tests the casting produced from the particular pattern was intended to represent only the first metal poured into a mold cavity that is "X" times larger. It is believed that the first stage of pouring is the most critical and that the metal filling the panel type casting represents to a satisfactory degree the fluid flow conditions which may exist in the early stages of pouring.

In view of the results obtained in these tests, the following observations and suggestions are submitted for further consideration:

1. Aspiration, while present, is not a detrimental factor in aluminum alloy gating systems unless turbulence is present. It is felt that within the limits of viscous or streamlined flow the metal flows inside a protective oxide-sleeve.

2. Water flow studies of gating systems do not seem to approach the conditions of metal flow in sand molds. In cases of vortex and aspiration, it is too sensitive a medium, and, in the case of molten metals in which entrapment of oxides or gas is important, the water medium is not sensitive enough. The effects introduced by partial solidification must also be overlooked in using a water system analogy.

3. It is suggested that a gating system used for additional studies of fluid flow incorporate the use of thin gates located at the center line of the runner, thus providing a skimming action on the metal entering the mold cavity.

4. A permeable sand mold does not behave the same as a nonpermeable mold with individual vents. A volume of gas aspirated

¹ Metallurgical Dept., Aluminum Co. of America, Bridgeport, Conn.

^{*} The large defect at the base of the sprue in Fig. J is a wet-sand blow.

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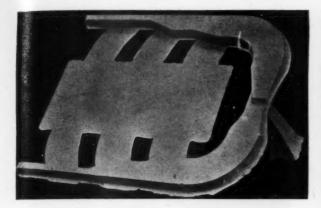


Fig. A-Gated casting from Test No. 1.

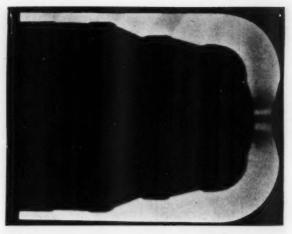


Fig. D-Reproduction of typical radiograph showing oxides and entrained gas found in runners of castings poured in Test No. 1.



Fig. B-Pouring box used in Test No. 1.



Fig. E-A gating casting from Test No. 2.

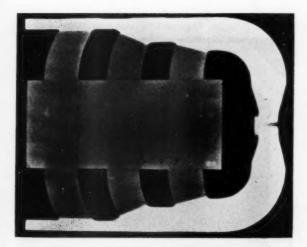


Fig. C-Reproduction of typical radiograph showing excessive oxides and entrained gas found in castings and gates poured in Test No. 1.

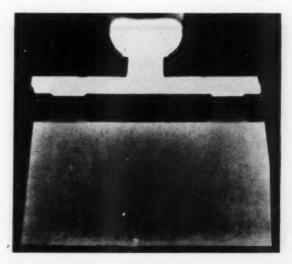


Fig. F-Reproduction of typical radiograph showing freedom from excessive oxides and entrained gas poured in Test No. 2.

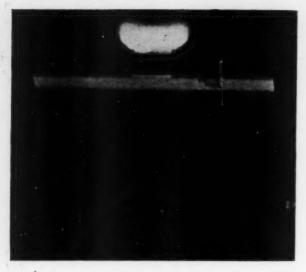


Fig. G-Reproduction of radiograph showing sound runner and sprue poured in Test No. 2.

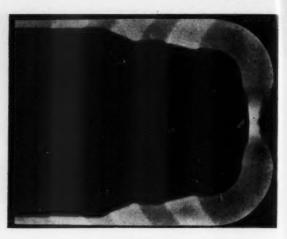


Fig. J-Reproduction of radiograph showing relatively sound runner poured in Test No. 3.

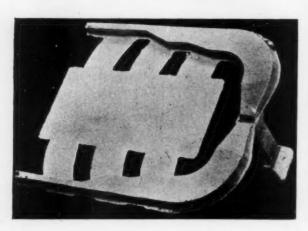


Fig. H-Gated casting from Test No. 3.

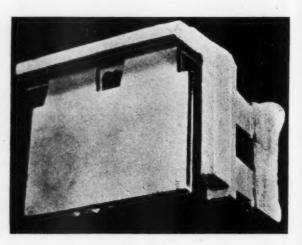


Fig. K-Gated casting from Test No. 4.

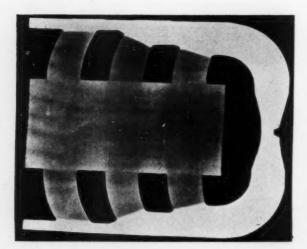


Fig. 1—Reproduction of typical radiograph showing sound casting poured in Test No. 3.

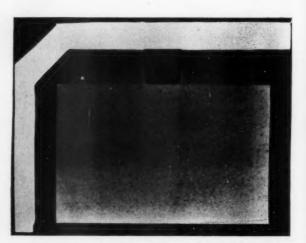


Fig. L-Reproduction of typical radiograph showing sound casting poured in Test No. 4.

APPENDIX-SUMMARY OF TEST CONDITIONS

| Test | Type of Pour Sprue | | Sprue D | esign | Runner | | Total Sprue Area | Total Runner Area | Total Gate Area | X-Ray Rating | | | |
|------|--------------------|---------------|-------------|---|--|----------|---------------------|----------------------|--------------------|-----------------|--|--|--|
| | A | В | À | В | À | В | | | | | | | |
| 1 | | X | | X | | x | 0.75 Sq In. | 1.50 Sq In. Max | 3.00 Sq In. | Poor | | | |
| 2 | X | | X | | x | | 0.52 Sq In. | 1.56 Sq In. | 1.04 Sq In. | Excellent | | | |
| 3 | X | | X | | | x | 0.61 Sq In. | 1.50 Sq In. Max | 3.00 Sq In. | Good | | | |
| 4 | x | | X | | x | | 1.22 Sq In. | 3.66 Sq In. | 2.44 Sq In. | Good | | | |
| • | | Tyr | oe of Pour: | A. : | Small basin d | lirectly | over sprue. | | | | | | |
| | | - /1 | | | | | h pour away fro | m sprue. | | | | | |
| | | Sprue Design: | | | A. Reverse taper approximately 5 deg per side. | | | | | | | | |
| | | -1 | | | Streamlined v | | | | | | | | |
| | | Runner-Ga | ate Design: | A. 1 | | | | | | | | | |
| | | | 0 | B. Streamlined with gates in cope and runner all in drag. | | | | | | | | | |

through a few holes may cause a stronger attack on the fluid than the same volume coming through the millions of holes present in a permeable mold. In the case of the permeable mold, a more gentle aspiration may take place and form a thin wall of air or gas that merely separates the fluid from the mold wall as long as turbulence is not present.

5. In gating design research, a study differentiating the effect of turbulence and aspiration should be considered.

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J. G. Mezoff and H. E. Elliott, "A Study of Factors Affecting Pouring Rates of Castings," Transactions, A.F.S., vol. 56, p. 279 (1948).

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7. R. E. Swift, J. H. Jackson, and L. W. Eastwood, "A Study of the Principles of Gating," Transactions, A.F.S., vol. 57, p. 76 (1949).

L. W. EASTWOOD (Written Reply to Messrs. Kearney and Witters): Messrs. Kearney and Witters selected a runner and gate design which was recommended for further study, not necessarily for foundry application. Actually, the study of the gating system, represented by Fig. A, D, H, I and K of their discussion, has been shown to be a faulty design when it was studied by the transparent mold, water, high-speed photography technique. Their poor results with this gating design, therefore, confirm rather than contradict the conclusions drawn as a result of the use of the transparent-mold technique. Because these investigators started on an erroneous premise, most of their conclusions are also erroneous. Further comment on their conclusions is as follows:

1. The calculation of Reynolds numbers indicate, in terms of the fluid dynamist, most or all metal flow is "turbulent" when pouring casting. However, this turbulence is not necessarily harmful, unless it is so violent as to rupture the oxide skin on the melt surface. Therefore, turbulence does not form dross unless it is sufficiently violent to entrain air or mold gases into the melt. Further, it has not been demonstrated that aspiration is not important when casting aluminum alloys, although it is admittedly only one of several ways in which the air or mold gases can be entrained into the liquid.

2. It is quite possible to select metal and casting conditions which will produce some castings of acceptable quality even with poor gating practice. It is an obvious error to assume that such constitutes proof that an unsound practice is sound. Tests with Wood's metal and with aluminum-10 per cent magnesium alloy melts in the Battelle foundry have shown excellent correlation with the observations made by using the transparent mold, water, high-speed photography technique. Only different gating practices, not individual design features, have been compared. Water appears to perform qualitatively similar to modern metals. The entrained gas bubbles escape from the water more quickly

than they do from the metal, but this obviously is unimportant. The important fact is that the gas is entrained in the liquid. If the liquid is aluminum or magnesium melt, dross will be formed, some of which may or may not be trapped before it enters the mold cavity.

All the observations indicate that the gating problem is over when the gating system has been completely filled. Kearney and Witters have also indicated this in the first paragraph of their summary. If so, it would be desirable that the gating system be filled before any of the metal enters the mold cavity. Obviously, this can be best accomplished by placing the gates in the cope and the runner in the drag. The latter must then be filled before any metal can enter the mold cavity. Once the runner has been filled, the possibilities for skimming action on the cope surface are then quite favorable. It is expected, however, that further data will be obtained on this feature of gating practice during future studies.

MR. ELLIOTT'S DISCUSSION

H. E. ELLIOTT (Written Discussion): 2 Dr. Eastwood's position that there is an ambiguity in the use of the expression "flowturbulence" is a point well-taken. Turbulence in the sense of chaotic motion of the individual molecules of the flowing liquid is of no concern; such motion does not produce casting defects. Only the flow is turbulent in the sense that there is intimate admixture of the flowing liquid with the surrounding atmosphere does "turbulence" become a source of casting defects. Turbulence of this type is damaging to the cleanliness of many liquid alloys. With these alloys, when fresh metal surfaces are repeatedly exposed to contact with the mold gases by unfavorable flow conditions, films of oxidation products are formed; these films are repeatedly folded into the flowing stream of liquid, and new surfaces are exposed to further attack. The result is the formation of a light "froth" or "foam," commonly called "dross" in foundry parlance. This phenomenon has been the primary subject of the investigations in this project to date.

The problem that is being studied—or at least one of the important problems—is the problem of preventing metal that has been damaged by flow-turbulence from entering the casting cavity. Logically, there are two approaches to a solution to this problem.

 By properly adjusting the geometry of the gating system, it is theoretically possible to eliminate flow-turbulence totally. Thus, no metal will be damaged by admixture with mold gases, and it will follow that only clean, undamaged metal can enter the casting cavity.

2. An alternate approach would be to tolerate some turbulence in early parts of the gating system, thus allowing some dross to form; then to effectively remove any dross so formed from the liquid stream before introducing it into the casting cavity.

The first approach—that of totally preventing gating turbulence—is the more theoretically appealing. The search for and discovery of methods for achieving non-turbulent flow is a very challenging and exciting pursuit. Our early work at Dow on this subject, in which we first predicted the sprue-aspiration effect, then demonstrated it by means of water flowing through a transparent mold, and finally confirmed that similar effects occurred when magnesium alloys flowed through sand molds, was of much interest. The early work in this A.F.S. project has

² Metallurgist, The Dow Chemical Co., Bay City, Mich.

been based very largely on this approach.

Dr. Eastwood's comments indicate, however, that some cognizance has also been taken of the value of establishing design conditions favorable to the separation from the metal stream of such dross as may have formed despite extreme care taken toward preventing turbulence. He stressed the value of confining the runner system to the drag mold, while placing the casting proper in the cope mold, so that the runner system will fill completely before metal begins to enter the casting cavity. This practice allows the light dross which forms in the early part of the gating system to rise and to be entrapped on the cope surfaces of the runner, thus keeping it out of the casting cavity.

Dr. Eastwood exhibited reproductions of radiographs of castings made by a "poor" method, as compared with castings made by a "good" method. It is noted that the "good" method differed from the "poor" method in two basic respects: the "good" method not only permitted less turbulence than the "poor" one: but provision was made with the "good" method to insure that the runner system would be completely full before any metal entered the casting cavity, while like precautions were omitted with the "poor" method. Thus, it was not possible to judge, from these radiographs, the independent effects of these two basically independent differences in practice. Close examination of the castings themselves, however, indicated the presence of considerable dross on the cope surfaces of the runners of both castings. While the runner of the casting made using "good" practice appeared cleaner than that using the "poor" practice. it appeared that the choking down of the base of the sprue with the "good" system may have produced a lower pouring rate. A lowered pouring rate would in itself reduce the tendency toward turbulence, independent of refinements in gating

RESEARCH SUGGESTIONS

The writer suggests that serious consideration be given to the following question, in planning the future course of this project: Despite the theoretical appeal of the approach to preventing turbulence defects by the method of totally preventing gating turbulence, is this approach likely to be the most rewarding? Is it likely to lead to the most practical solutions to the problem of turbulence defects, from the point of view of the practical foundryman?

The foundry with which the writer is associated produces magnesium-alloy castings, largely for aircraft applications. Of necessity, we are extremely conscious of the degree of freedom of our castings from dross and inclusions. Very rigorous visual, fracture, and radiographic inspection methods are applied to insure that our rigging is producing castings free of inclusionsboth before the job goes into production and periodically during each production run. Despite the high tendency of our alloys toward drossing, and despite these high inspection standards. we have a very low rate of rejection of castings for defects traceable to gating turbulence. It is emphasized that this is accomplished without resorting to elaborate or precise runner cup designs, the use of plugs in the top of the sprue or temporary dams across the runner, the use of precise or unwieldy crosssectional shape factors or taper factors in the sprue design, or other particularly arduous practices in the design of the early part of the gating system. We do employ filtering provisions in the runner system, at some point after the metal has attained its lowest level in the mold. We do take pains to fill the runner system completely, when possible, before allowing metal to be introduced into the casting cavity. We do take certain other precautions; such, for example, as using as low a pouring rate and pouring temperature as is consistent with achieving satisfactory quality in other respects; and such as keeping the total runner area and the total ingate area each equal to at least double the sprue cross-sectional area, so that there is no "nozzle" or "jet" effect anywhere in the runner system after screening has been accomplished. None of these practices introduces undue molding complications, or tends to unusual costs; nor is extreme precision in the inter-relation of the relative dimensions of the various portions of the gating system a necessity. We would be very reluctant to relinquish these positive advantages of our gating practice in favor of a "turbulence-free" system that would be appreciably more expensive to apply.

In another important respect, our gating practice would differ from the "good" system shown in the fluid flow motion picture film. We would provide a "pop-up" or "relief" at the extreme end of each branch of the runner, at the point farthest from the sprue. This would serve to prevent the entrapment of a pocket of air at the end of the runner which might produce an explosion of the mold. It would serve also to relieve the violence of the entry of the first metal into the ingates after the runner has completely filled.

Also, we would consider it almost unthinkable to provide for no venting of the casting cavity itself. If the casting cavity is not vented, the mold gases in this cavity may not be able to escape fast enough to permit smooth and undisturbed entry of the metal into the cavity. Further, provision of a vent avoids the penetration of the metal into the pores of the sand, which may result if the kinetic energy of the metal is absorbed suddenly at the moment of complete filling of an unvented cavity.

The writer would greatly desire to see an attempt made in the future course of this project to demonstrate a method of introducing undamaged metal into the casting cavity based on the separation of dross formed by turbulence in early parts of the gating, prior to the entry of the metal into the casting cavity. He would like to see a determination made of how much turbulence could be tolerated in early parts of the system, by the intelligent application of provisions for the separation of dross from the metal stream. He would not be surprised if the most practical means of solving the problem of turbulence defects should be uncovered by researches directed along these lines.

L. W. Eastwoon (Authors' Reply to Mr. Elliott's: Mr. Elliott's comments are worthy of careful consideration. There are a few points which should be made, however, as follows:

1. We do not believe that damaged metal can be entirely eliminated during the initial part of the pour. Our aim is to minimize it. It is like protecting a good apple from damage. The alternative is to let the apple be damaged and then attempt to restore it after it is rotten.

2. Mr. Elliott's sixth paragraph is entirely correct. To date, we have not had the opportunity to check in the foundry each design feature of the gating systems. Instead, we compared representative gating systems which, of course, differ in more than one detail.

3. Although a final gating design has not yet been established, the developments to date indicate that most or all of the gating design features can be incorporated permanently on the pattern. These are actually not so complicated as Mr. Elliott indicates. Usually, sound castings can be made without resorting to temporary metal dams placed across the runner. These were used in the motion picture film to show more clearly the desirability of filling the gating system quickly. On the contrary, most foundries would regard the complicated system of cylindrical screens and steel wool, recommended by Mr. Elliott, as being rather elaborate but not foolproof. Furthermore, such gating systems cause iron contamination, particularly in aluminum, and invariably require high pouring temperatures with a host of evils resulting therefrom, as Mr. Elliott is, no doubt, familiar. Many foundries in the United States and in England have demonstrated that, by using a good gating technique, dross-free castings can be made without resorting to screens and steel wool and their attendant disadvantages. While screens and steel wool do have a place in the foundry, it seems more logical that, first, the gating geometry should be such as to minimize damaged metal, and then, in the few instances that it is necessary, use steel wool or screens, or both, to catch the small amount of dross formed in the first splash of metal through the gating system. Actually, in many instances, the perforated screen merely serves as a device to break up the dross into small particles rather than catching it. This will always happen when dross is forced through the screen under some pressure.

4. Mr. Elliott refers to the importance of vents. The mold cavity of the Lucite molds, of course, was thoroughly vented through the cope by several hundred small-diameter holes. In the foundry tests, vents were used both in the ends of the runner and cope through the mold cavity. Actually, with the same permeability of about 20, it was found that such vents appeared to have no particular beneficial effect but they were used because it is generally good practice to do so.

5. Some attention will be paid to the use of screens in the future work. Perhaps, the most useful function of perforated screens is that they provide a means of reducing the velocity of the liquid in the runner and provide a back-pressure, which

also helps to reduce the entrainment of mold gases into the liquid on the sprue side of the screens. It appears, however, that even these effects can be accomplished readily by a proper design of the sprue base.

T. B. Belffeld (Written Discussion): ³ To advance into practice the fundamental study of the principles of gating and risering as sponsored by the American Foundrymen's Society and Battelle, it is an imperative necessity that each of the many foundry variables are brought to within a very narrow and practical range.

Naturally, the ingot should be to specification. The furnace atmosphere should be neutral or slightly oxidizing, and contain no reducing gases or water vapor. The metal not overheated, as contrary to the general laws of physical chemistry governing the solubility of gases in liquids, it should be remembered that the solubility of gases in liquid metal increases with increasing temperature. The transfer of the metal to the pouring ladle without splashing or turbulence. The melt should not be chilled by the immersion of gates or risers. Next, the sand should have a moisture content of approximately 5 to 6 per cent, and a permeability of 12 to 20.

There are almost an endless number of variables in aluminum founding, however, after these briefly mentioned variables are brought under control and constantly checked, the next factor which affects the quality of castings and reduction of scrap, is the study of the "Principles of Gating, Risering and Chilling."

Inasmuch as a majority of our aluminum castings receive a satin finish, castings with any porosity are not acceptable.

Our problem was surface porosity, invariably located on the cope surface and only became apparent after sanding and wire brushing. Checking showed that this condition differed from dispersed pinhole porosity. Cutting through pieces of uniform sectioned castings did not show pinhole porosity uniformly dispersed throughout the whole casting. The condition was spasmodical and at times reached epidemic proportions.

At this time, the first findings of the A.F.S. Battelle project had not yet been released, therefore, the principles of fluid dynamics were applied from a scientific, rather than a trial and error approach, but the results lacked satisfactory uniformity. Subsequently, the film "Fluid Flow" was released by A.F.S. and after a very thorough study of these principles and several conferences with Dr. Eastwood, progress was becoming apparent and results recompensing.

A detailed discussion of these fundamental details will not be gone into at this time, as there has been appropriate literature published on the subject that may be studied.

Our satisfactory results based on this project proved conclusively:

1. The interdependence of riser and gate.

2. To maintain an adequate head of metal at the top of the sprue to eliminate a vortex and avoid air entrainment.

3. Place the runner in the drag and the ingates to the mold cavity, in the cope.

4. Prevent metal from spurting, squirting, splashing, turbulence and cascading.

5. That the yield of the castings poured as compared to the amount of the metal poured can not be brought to any comparable or desirable parity.

In conclusion, it is the sincere hope of the writer that further work on this project will be continued jointly by A.F.S. and Battelle to include gating systems of different vertical and lateral dimensions and other methods of avoiding the entrance of damaged liquid into the mold cavity.

R. E. COPE (Written Discussion): 4 Before one can enter into a discussion of the application of theoretical principles to the design of gates for the flow of metal through sand molds, there are certain fundamental principles which must be fully understood. They are as follows:

1. Newton's first law of motion: Every body continues in its state of rest or uniform motion in a straight line except as it is compelled to change that state by force impressed upon it.

2. Newton's second law of motion: The time rate of change in the linear momentum of a body is proportional to the force acting upon the body and the change takes place in the direction of the force. For our purposes these two laws resolve themselves into a fundamental equation as follows:

$$F = \frac{M \ V^z}{2 \ g} = \frac{P}{Q} \text{ or } V^z = 2 \ g \ H$$
Where $V = \text{Linear velocity}$

$$g = \text{Acceleration due to gravity}$$

$$H = \text{Fluid head}$$

$$P = \text{Pressure}$$

$$Q = \text{Density}$$

3. First law of thermodynamics: The sum of all the energies in an isolated system remains constant.

This third law when applied to fluids in motion resolves itself into an equation known as Bernouli's theorem as follows:

$$\frac{V^{z}}{2g} + \frac{P}{Q} + F + X = Constant$$
Where
$$\frac{V^{z}}{2g} = Velocity head$$

$$\frac{P}{Q} = Pressure head$$

$$\frac{P}{Q} = Friction head$$

$$X = Potential head$$

When a specific construction and location of the pattern in the mold and the details of the construction of the mold itself are known, the above principles may be used in developing a proper gating system which allows the metal to run uniformly and quietly into the molds.

APPLICATION OF PRINCIPLES

In the application of these principles to the design of gates for sand castings, let us start at the metal flowing out of the ladle and into the pouring cup.

When the molten fluid enters the pouring cup it has already attained a velocity head equal to the distance between the pouring ladle and the top of the mold. Increasing this distance increases the force upon the metal in the form of velocity head.

As the metal flows down through the down sprue, it is gathering energy due to the force of gravity, as explained in Newton's second law. When the metal makes the turn at the parting level of the mold it has attained its maximum energy content. It is losing energy slowly in the form of potential heads such as loss in viscosity due to the drop in temperature and frictional heads due to the drag of the mold walls themselves and the turbulence due to turns in the gating system. Other than these small losses in energy, the total internal energy of the fluid remains constant from this point on.

If there is no constricting medium in the bottom of the pouring sprue (such as a strainer gate), even though the gating system appears full and choked at the top, the metal will constrict when falling freely through this gating system just as the metal stream constricts when poured from a ladle out in the open. This constriction occurs because the metal is gaining energy, gaining velocity in a linear direction, but since no metal is being added, it is reducing itself in cross-section and pulling itself away from the walls of the down sprue. This is what is known as an aspirator or sucking action which pulls air into the metal when traveling down the down sprue. Therefore, some kind of a uniform choke such as a strainer core is always recommended at the base of the down sprue. The hole sizes in the strainer core should be varied to produce the velocity desired.

Let us assume we have a gating system with one long runner of 1 in. cross-sectional area with four equally spaced ingates each having 1/4 in. of cross-sectional area. This gives us equal area in ingates and runner.

When this system comes to equilibrium, it is obvious that the volumetric velocity or the cubic volume of metal passing through

³ President, Cochrane Foundry Inc., York, Pa.

Michigan Wheel Co., Grand Rapids, Mich.

point V_0 is equal to the sum of all of the other volumetric velocities 1, 2, 3, and 4 because the cross-sectional areas of the four ingates is equal to the cross-section at point V_0 .

However, at point V_4 , the forward motion of the metal has been absolutely stopped. The forward velocity has been brought to zero. Therefore, according to the conservation of energy, the pressure becomes a maximum. The velocity through gate 4 is high, in proportion to the high pressure of the material because this pressure is all that makes the metal flow in a direction other than the original direction of flow, as explained in Newton's first law. Proceeding down the gate towards the down sprue to point V_3 , there is an internal pressure to the material which is somewhat lower than it was at point four because the material is still in forward motion. Since the internal pressure is lower, the velocity into the mold and out of point V_3 is lower than it was in point 4. The same is true at point 2. The velocity in the main stream is a little higher than it was at point 3, therefore, the internal pressure is lower and the velocity into the ingate is lower than at 3. The same may be said of point V_1 .

As a matter of fact, in actual experimental evidence, I have seen the velocity so high at point 1 and the internal pressure so low that, as the back pressure due to the filling of the mold increases, the flow of metal through gate 1 actually reversed itself and flowed back out of the mold into the runner and on down to gate 4 and into the casting a second time.

Now the question arises as to what to do to gating designs to correct this situation. It is apparent from our fundamental law of Bernouli that in order to hold the internal pressure of the metal constant, we must also hold the linear velocity in its original direction constant because we have seen if we allow this linear velocity to drop, the pressure goes up and the amount of metal running into the adjacent ingate increases over what it should.

Now let us redesign this gating system as follows:

This system is designed so that the cross-sectional area of the runner is equal to the cross-sectional area of the remaining gates only. In other words, the area at V_0 is equal to the sum of the areas of all of the ingates. At the far side of V_1 the runner is constricted so as to have an area equal to the areas of ingates V_4 , V_3 and V_2 . At the far side of V_2 , the runner is again constricted to have an area equal to the area of the ingates of V_4 and V_3 , etc.

When this system comes into equilibrium which is very soon after pouring begins, it can easily be seen that when the metal gets down to ingate V_4 its velocity drops to zero as it hits the end of the runner. Its internal pressure increases and creates a velocity into the ingates. At V_3 the fluid has also hit a barrier which has stopped dead a portion of metal increasing the pressure forcing metal into gate 3. The same is true on up through gate 2 and 1.

Equilibrium is established when linear velocities, 1, 2, 3, and 4 are equal, and equal to the linear velocity V₀. Cross-sectional area of the runner has been constricted so that the linear velocity of the molten fluid flowing through is maintained. As long as the linear velocity is maintained, the pressure will remain low. The pressure stays constant, the linear velocity stays constant and consequently, the flow through each individual ingate stays constant.

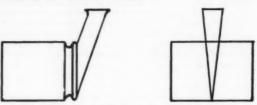
This rule applies very nicely except for one little flaw. We have neglected friction head and potential head which covers loss of temperature and increased viscosity. Both of these drags on the metal are proceeding.

There is no adequate data to accurately calculate the amount of pressure lost due to friction and temperature decline. However, by assuming these as constant and developing a rough gating system using merely the velocity and pressure head principles as stated in Bernouli's theorem, any gating system can be designed and come very close to the exact theoretical gate required. Then after the gate has been moved into production, minor adjustments may be made to further compensate for the loss of pressure due to friction losses and temperature drop energy losses.

This same set of principles may be applied equally well to the gating of permanent mold. However, it becomes more complex simply because the metal is moving in a vertical direction through the down sprue and is gathering energy at the same time that you are trying to control it.

However, the author has found that by applying the three principles above to permanent mold gating properly balancing the effects of the different forces upon the molten material, that permanent mold gates may be designed which will uniformly distribute the metal up the casting, that is, as the level of the metal in the mold cavity moves up.

We will use an ordinary vertical casting with a knife gate all the way up the side as an example.



As you will note at the bottom, if we start with a uniform long ingate of uniform cross section and have a down spruerunner on to the side of it which starts with no cross-sectional area at the bottom, it increases in cross-sectional area roughly twice as fast as the cross-sectional area of the gate below the point in question. This gating design will give exactly the satisfactory results desired.

By starting out with a slow, easy pour, the metal is gently entered into the bottom of the mold. Then by increasing the velocity of the pour as the mold fills, this form of gate will force the metal to flow into the top part of the mold and allow hot metal into the top. It will also prevent formation of hot spots at the bottom which is the common plague of permanent mold casting, especially pistons when poured head down.

mold casting, especially pistons when poured head down.

The author has used these principles and equations in his gating design in sand and permanent mold over the last six years. Every job that has been gated, has been gated using these principles so as to put the metal exactly where you want it. This allows the actual velocity of the metal moving into the casting to be much lower. There is no spruing, no high velocity ingates. This prevents aspiration and the accumulation of air and other drossy conditions. It spreads out the distribution of the metal into the mold and prevents formation of hot spots, reducing shrinkage problem.

These same principles have been applied by the engineers who design sewage systems, by heating and ventilating engineers, by all people who deal in air and water and low temperature fluids. I feel that it is time that the foundrymen take these same principles and apply them to their own work.

It is suggested that you investigate a few heating systems and note that the main air pipe conductor is gradually reduced in area by the cross sectional area of each heating outlet as it is taken off the main line. This is the most positive evidence possible of how these principles work.

MR. KEARNEY: I would like to comment on the written discussion we have submitted. We chose alloy 356 because it does have a very tenacious film and serves to retain any oxide bubbles in the casting.

One of the conclusions read as follows: Permeable sand mold does not behave the same as a non-permeable mold with individual vents. In the case of the permeable mold, a more gentle flow may take place and form a thin wall or area of gas . . . as long as the fluid is not moving fast enough to tear the oxide from the wall of the runner to the gating system. I would like to have Dr. Eastwood's comments on that.

Dr. EASTWOOD: Mr. Cope's comment is very much to the point. I did not emphasize that a real problem of gating design is the one which involves dissipation of energy. The flow of velocity of the liquid as it emerges from the base of the sprue is high and at a maximum. The problem is to reduce this flow velocity in feet per second by the time the liquid enters the mold cavity. This flow velocity with which it emerges from the base of the sprue. The problem of gating design is to absorb this energy without giving opportunity for the entrainment of gas into the liquid stream. The last time I talked to Mr. Marzke at the

Naval Research Laboratory they were finding the same problem was the most difficult to solve.

Mr. Kearney brought up the question of alloy selection for foundry test. It should be emphasized that the greater the tendency the melt has to the formation of dross, the more acute the gating problem. When casting beryllium, the gating design and pouring technique must be nearly perfect to avoid dross. When pouring gray iron, most any gating design will get by. When selecting a material to establish the degree of perfection of the gating design, it is desirable, therefore, to choose an alloy which is quite adversely affected by turbulence and the formation of dross. Accordingly, we selected an aluminum-base alloy containing 10 per cent magnesium. We would regard alloy 356 as being much less sensitive to the turbulence and formation of dross.

From the standpoint of venting, the lucite mold is similar to a permanent mold. We could drill holes into the gating system at any location of interest. Consequently, the aspiration could be studied precisely at any location which was selected. It is true that the aspiration through one vent hole will effect the amount of aspiration through a second vent hole placed at a location further down the flow channel. Several vent holes are, therefore, not an exact quantitative duplication of the permeability obtainable in sand molds. It probably is similar qualitatively, however. In all probability, the aspiration through drilled vent holes in lucite is more severe than the aspiration in a sand mold, all other factors being the same.

However, there is one other consideration when pouring hot metal into sand molds. Mold gases are generated under some pressure, depending upon the degree of permeability of the mold. In the lucite mold, aspiration is caused by the suction of gas under one atmosphere. When pouring sand castings, aspiration may occur with some gas pressure which would facilitate

the gas entrainment.

W. S. Pellini: ⁵ There are several unrelated investigations of the study of the fluid flow of liquid metal under way. Results reported by Battelle in this paper have interesting parallels to work performed under the auspices of the Navy at the Naval Research Laboratory. I would like to say that certain of the Naval Research Laboratory findings relative to the flow of steels substantiate the results obtained by Battelle with water analogue methods.

There are two stages to be considered in the introduction of metal into a mold. During the first stage the gating system is only partly filled with the onrushing liquid, chance deflections from side to side of the gate wall and reflections of pressure pulses due to impingement at corners and runner ends are prevalent. This stage is quite turbulent, as Dr. Eastwood pointed out

In the subsequent stage sufficient back pressure is built up to cause a general pressurization of the gating system. This stage is usually fully developed only in castings that have a fair thickness. The work performed by BMI has been on fairly thin slabs and the work we did concerns rather heavy sections. Consequently, the first stage conditions of pouring are particularly dominant in the BMI tests.

Since the first stage of pouring does develop considerable turbulence it should therefore lead to dross formation for metals prone to such. We have found that gating practices which provide for a long first stage period are particularly bad in this respect. However as Dr. Eastwood pointed out, metals such as iron and steel, which do not dross are relatively unaffected by the gating practice used except, I should add, possibly in respect to

cold-shut formation with thin castings.

You have noted from the BMI motion picture film that during the first stages of flow the first lateral gate was ingored by the liquid metal which proceeded to the farthermost gate prior to exit into the mold. We have experimentally determined the same effects with liquid steels. Also, that closing down the runner systems, or chocking, so to speak, will improve flow conditions and produce uniform flow from all gates of most finger systems by virtue to backing up of the metal and developing a general pressurized condition.

We have, however, noted a disturbing thing concerning uniform flow obtained in this manner. With the increase velocities a very strong jet effect is also obtained which fact concerns us inasmuch as the desirable pressurization of the system may cause a disruption of the sand which will then be trapped as pockets in the casting surface. Thus, enforced chocking such as BMI resorts to by means of tapered sprues may not be entirely bene-

ficial in the long run.

In conclusion, I would like to call for a little caution in experiments to check the validity of the fluid flow findings that have been made to date. The experimental work and interpretation by Battelle and by the others has been cautious in keeping with the novelty of the approach. The work performed by operating persons in the foundry to check the validity of our findings should likewise be cautiously carried out so as not to give a black eye, so to speak, to this work by possible misapplication of the principles involved.

Given a fair chance, we believe that the findings which have been brought out by these investigations should prove to be of

real value to the practical man.

⁸ Head, Metal Processing Branch, Naval Research Laboratory, Washington, D. C.

SURFACE HARDENING OF PEARLITIC MALLEABLE IRONS

Final Report

By

S. H. Bush,1 W. P. Wood2 and F. B. Rote3

ABSTRACT

Nine pearlitic malleable irons exhibiting wide differences in chemical composition, production practices, and microstructures have been investigated for response to surface hardening heat treatments. Flame and induction (3000, 9600, and 300,000 to 347,000 cycle) heating, followed by oil, water or spray quenches have been used for the surface hardening treatments. Case hardness and depth were determined by superficial Rockwell or Rockwell C measurements and metallographic examination of etched specimens.

On the basis of a hardness of 50 Rockwell C at a minimum depth of 0.060 in., eight of the pearlitic malleable irons investigated could be satisfactorily induction hardened with 9600 cycle and seven with 3000 cycle. At high frequencies (300,000 to 347,000 cycles) two irons were consistently unsatisfactory. Irons flame hardened at an indicated temperature of 1500 F (815 C) were generally satisfactory, with one exception, when quenched with water. With an oil quench the hardness of the cases was below the specified value of 50 Rockwell C at 0.060 in. Comparison of the response of the irons to various forms of surface hardening treatments is given graphically. The irons are also compared on the basis of chemical composition.

Of the structures investigated, one, a dense pattern of extremely small spheroids of cementite, responded most satisfactorily to all types of treatment.

Introduction

AN EXAMINATION OF THE LITERATURE revealed that no systematic investigation of the response of pearlitic malleable irons to surface hardening has been made. Such information is desirable, due to the increasing use of surface hardened pearlitic malleable irons. This paper is a final report of such an investigation, using nine different pearlitic malleable irons supplied by the following foundries:

- 1. Belle City Malleable Iron Company
- 2. Central Foundry Division, General Motors Corporation

- 3. Chain Belt Company
- 4. Deere and Company
- 5. Link-Belt Company

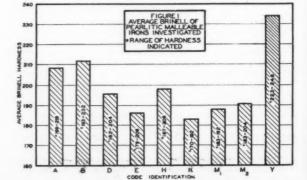
6. National Malleable and Steel Castings Company. All irons were the standard products of the participating foundries and manufactured to conform with ASTM specification A220-48T. They were selected to represent the entire range of microstructure, chemical composition, and malleabilizing cycles normally

cal composition, and malleabilizing cycles normally experienced in pearlitic malleable irons. Table 1 and Fig. 1 show the properties of the as-received samples.

The purpose of the investigation was to determine whether or not there is an optimum microstructure for surface hardening such irons. Variables examined were type of heat treatment (i.e., flame and induction hardening at high or low frequency), rate of power input, time of heating, surface temperature, and type of quench. These variables cover the normal industrial conditions encountered in the surface hardening of pearlitic malleable irons. From these data it is possible to determine whether an optimum microstructure exists for a specific hardening condition, or for all hardening conditions.

Materials

A total of nine irons were supplied by the participating foundries. These were coded A, B, D, E, H, K, M₁, M₂, and Y. Metallographic examination revealed that the nine irons could be grouped into four broad microstructural classifications, but that each iron dif-



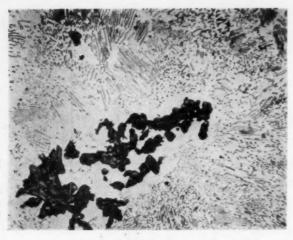
This report covers a research project initiated and sponsored by the Research Committee of the Malleable Division of the American Foundrymen's Society. Members of this Committee are as follows: C. F. Joseph, Chairman, W. D. McMillan, Vice-Chairman, H. Bornstein, W. A. Kennedy, J. H. Lansing, C. F. Lauenstein, R. Schneidewind and Milton Tilley.

² Research Fellow, Department of Chemical and Metallurgical Engineering, ² Professor of Metallurgical Engineering, ³ Associate Professor of Metallurgical Engineering and Metal Processing.

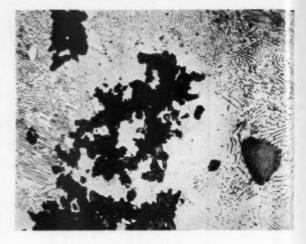
University of Michigan, Ann Arbor, Mich.

TABLE 1-PROPERTIES OF AS-RECEIVED PEARLITIC MALLEABLE IRON BARS

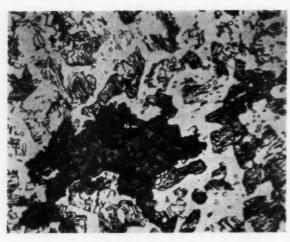
| Typical Analysis Mn P S | alysis | 0 | Cr P. | Melting Procedure | Malleabilization Cycle | Microstructure | Average Brinell Hardness | Brinell Bradness | ASTM Classification* |
|--|--------|----------|---------|-------------------------------------|--|--|--------------------------------|---------------------|-------------------------|
| 90.0 | 0.12 | 2 0.025 | | Duplex (cupola- electric) | 12 hr at 1750 F, air quench Draw at 1225 F | Bullseye 50% fine-medium pearlite 50% spheroidized cementite | 208 | 199-219 | 48005 |
| 1.47 0.48 0.09 0.0015% Boron at ladle | 0.15 | 5 0.03 | | Duplex (cupola-air) | 16 hr at 1750 F Still air from 1600 F Draw 1240 F for 6 hr | Bullseye 70% medium pearlite 30% spheroidized cementite | 16 | 193-232 | 48005 or 60003 |
| 0.15 | 0.122 | 22 0.017 | | Air furnace | 50 hr to 1600 F, 45 hr at 1600 F. Cool to 1320 F in 32 hr, hold between 1270 F-1320 F for 24-hr. Cool from 1270 F-1200 F in 8 hr | Some massive cementite No bullseye 75% spheroidized cementite Small islands of medium pearlite | 9 | 182-204 | 48005 |
| 0.16 | 90.0 | 92 | - | Air furnace | Completely malleabilized, followed by heat to 1475 F 15 min and oil quenching. Draw—1200 F | No bullseye Spheroidized or chain-like structure of carbide at ferrite grain boundaries | 187 | 179-206 | 43010 |
| 0.12 | 0.15 | 70 | | Triplex (cupola air-electric) | lst stage 1800 F; oven 187-206 cooled to 1400 F, 4°/hr, 1400- 1300 F, Total time, 100-140 hr | Bullseye Medium pearlite surrounded by fer- rite. Some spheroidized cementite. Many inclusions | 861 | 187–206 | 48005 |
| 91.0 | 0.10 | | 0.015 A | Air furnace | 50 hr to 1600 F, 45 hr at 1600 F, Cool to 1320 F in 32 hr, hold be- tween 1270 F-1320 F for 24 hr Cool from 1270 F-1200 F in 8 hr | No bul, eye 80-90% spheroidized cementite, a few scattered lamellae | 183 | 170-192 | 43010 |
| 1.05 0.50 0.135 (0.01% Boron Treated) | 0. | | | Duplex (cupola-air) | 1800 F max, 30 hr above 1500 F oil quench, draw 2½ hr at 1280 F | Completely spheroidized. Very irregular graphite | 188 | 189-192 | 43010 |
| 1.05 0.75 0.135 (0.01% Boron Treated) | | | | Duplex (cupola-air) | furnace cool | Large bullseyes Pearlite-coarse and largely decom- | 161 | 183-204 | 43010 |
| | | 0.12 | 620.0 | Cupola (cupola electric) | 12 hr at 1590 F air queiteitei 110in 1650 F. Reheat to 1550 F. Hold 45 min, oil quenched, draw 3 hr at 1230 F | posed, Mainly spheroidized cementite No bullseye Dense pattern of finely spheroidized cementite. | 234 | 223-248 | 60003 |



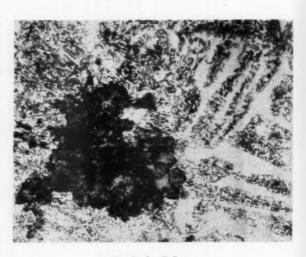
A-Code A Iron



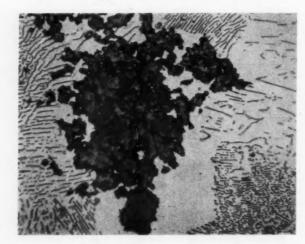
B-Code B Iron



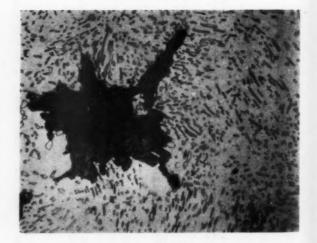
C-Code D Iron



D-Code E Iron

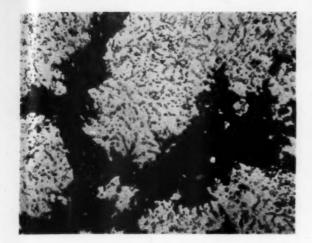


E-Code H Iron

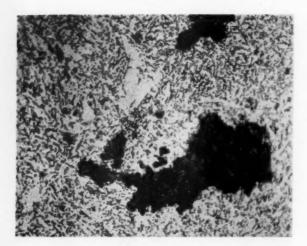


F-Code K Iron

Fig. 2-Typical matrix microstructures of pearlitic malleable irons investigated. Picral etch. 1000X.



G-Code M₁ Iron



I-Code Y Iron

fered in some respect from the others. The four general classifications were:

1. Pearlite combined with spheroidized cementite, plus a bullseye structure (Irons A, B, H, M₂).

2. A ferritic matrix with small areas of medium pearlite and a large quantity of spheroidized cementite (Iron D).

3. A spheroidized matrix with chains of iron carbide at the grain boundaries (Iron E).

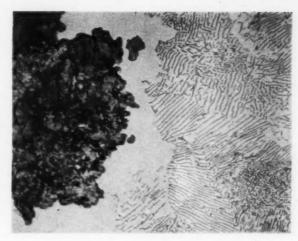
4. A spheroidized cementite structure (Irons K, M₁, Y).

The specific microstructures differed from these broad classifications in certain respects. These specific structures were:

1. Iron A-About 50 per cent fine to medium pearlite and 50 per cent spheroidized cementite plus a definite bullseye structure.

2. Iron B—About 70 per cent medium pearlite and 30 per cent spheroidized cementite plus small areas of massive cementite together with a bullseye structure.

3. Iron D-Small islands of medium pearlite surrounded by spheroidized cementite amounting to 75 per cent of the whole. No bullseye structure.



H-Code M2 Iron

Fig. 2-Continued

4. Iron E-Spheroidized or chain-like structure of carbides at ferrite grain boundaries. No bullseye structure.

5. Iron H—Medium pearlite together with small areas of coarse pearlite surrounded by ferrite. Some spheroidized cementite, and a bullseye structure. Many inclusions in the matrix.

6. Iron K-A coarsely spheroidized structure with pearlite 80 to 90 per cent decomposed. No bullseye structure.

7. Iron M₁—Completely spheroidized. Spheroids coarse and comparatively widely separated. No bullseye structure. Graphite large and very irregular.

8. Iron M₂-Pearlite coarse and largely decomposed with the microstructure made up mainly of spheroidized cementite. Large bullseyes; some inclusions.

9. Iron Y-Structure completely spheroidized. Spheroids small and pattern dense. No bullseye structure. These microstructures are shown in Fig. 2 at 1000 diameters.

The nine irons could also be grouped into four broad classifications on the basis of graphite size, shape, and distribution. These structural classifications, when compared at 100 diameters were:

1. Graphite size $\frac{3}{8}$ to $\frac{1}{2}$ in. in diameter, regular in shape, 1 to 3 pieces of temper carbon per square inch. Three irons, A, B, and Y have such a structure.

2. Graphite size ½ to ¾ in. in diameter, irregular in shape with some tentacles, ½ to 1 pieces of temper carbon per square inch. Irons D, E, H, and K have this structure.

3. Graphite size 1 to 2 in. in diameter, very irregular with several tentacles, $\frac{1}{4}$ to $\frac{1}{2}$ pieces of temper carbon per square inch. Iron M_1 has this structure.

4. Graphite size $\frac{9}{8}$ to $\frac{9}{4}$ in. in diameter, regular structure and usually two pieces of temper carbon adjacent, $1\frac{1}{2}$ to $2\frac{1}{2}$ pieces of temper carbon per square inch. Iron M_2 has this structure.

The nine microstructures are shown at 100 diameters in Fig. 3. Examination of these nine photomicrographs will indicate how well they fit the above

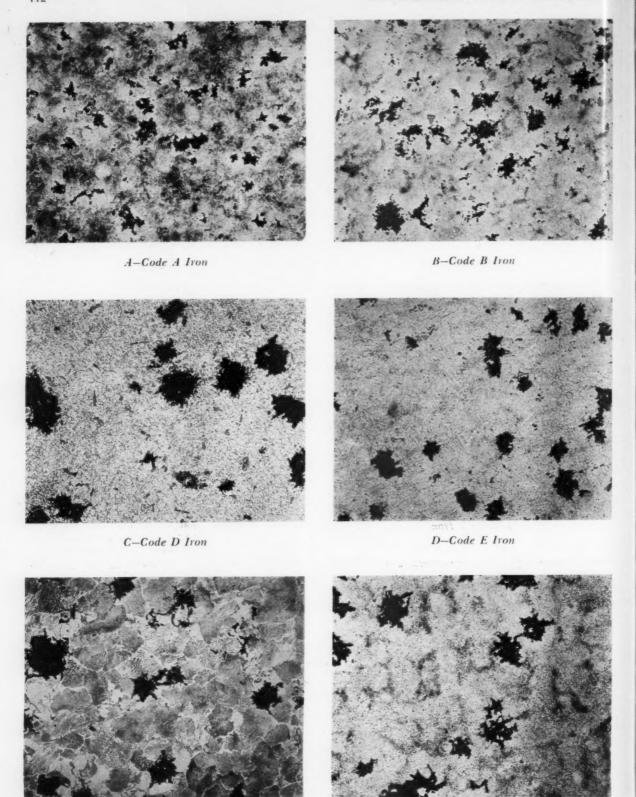
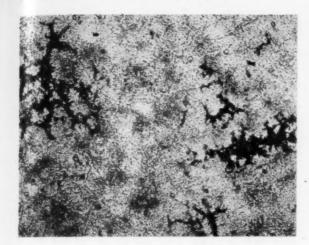


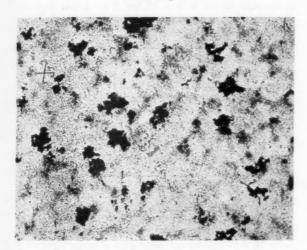
Fig. 3-Typical graphite size, shape and distribution of pearlitic malleable irons investigated. Picral etch. 100X.

E-Code H Iron

F—Code K Iron



G-Code M, Iron



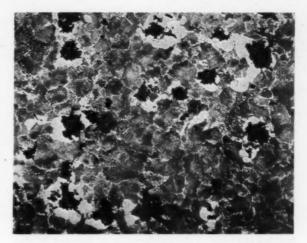
I-Code Y Iron

classifications.

Prior to heat treatment the bars were machined to 1-in. diameter from an as-cast diameter of 1½ to 1¾ in. This gave a uniform, smooth machined surface for the heat treatment. The bars were 8 in. in length after machining.

Equipment and Procedures

3000-Cycle Induction Heating—Two series of samples were run; one in a 50 kw unit at the University of Michigan, and the other in 150 kw unit at the Tocco Division of the Ohio Crankshaft Co. The University equipment consisted of a 3000-cycle motorgenerator unit coupled to a 6-turn copper coil in series with a 20-turn ballast coil. Tests were made at 28, 34, and 38 kw at times varying from 7 to 40 sec. All bars were quenched into a tank of highly agitated water. The second unit, the 150 kw experimental motorgenerator at Tocco Division of the Ohio Crankshaft Co., was coupled to a single turn copper coil, which heated a zone about 1 in. in width. This coil was coupled with a perforated plate through which the samples could be spray quenched. Three tests were



H-Code M2 Iron

Fig. 3-Continued

run on each bar, permitting a better comparison of response to hardening, since the differences from bar to bar were eliminated. Test bars were heated at a power input of 25 kw for 7 sec; 50 kw for 3.5 and 4.0 sec; 75 kw for 2.25 sec; and 100 kw for 1.5 and 2.0 sec.

9600-Cycle Induction Heating—A 150 kw experimental motor-generator unit at the Tocco Division of the Ohio Crankshaft Company was used for all tests made at 9600 cycles. The same single-turn coil used at 3000 cycles was also used at 9600 cycles. Samples were spray quenched, as in the case of the 3000-cycle unit. Three tests were run on every bar, permitting a better comparison. Test bars were heated at a power input of 32 kw for 3, 4, and 5 sec; 50 kw for 2 sec; and 75 kw for 1.5 and 2 sec.

High-Frequency Induction Heating—Two different high-frequency units were used to test the response of the irons to heating at frequencies of 300,000 to 347,000 cycles. A high-frequency induction heating unit delivering a maximum of 30 kva was used with a 5-turn copper coil. The bars were rotated in the coil for 11.5 and 12 sec, and then quenched into a tank of violently agitated water. Further tests were made with a high-frequency generator operating at 300,000 and 347,000 cycles. A single turn copper coil was used in conjunction with a spindle for rotating the piece and a perforated plate through which the bars could be spray quenched. A zone of about 1 in. in width was heated so three tests could be made on each bar. Test bars were heated at:

60.1 kva, 300 kc, for 3.0 and 3.5 sec

35.5 kva, 347 kc, for 5.0 and 5.7 sec

51.5 kva, 347 kc, for 3.5, 4.0, 4.5 and 5.0 sec

91.8 kva, 347 kc, for 1.5, 1.75, and 2.0 sec

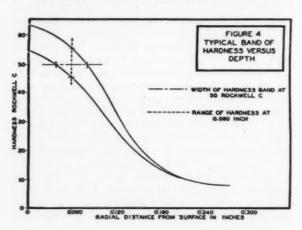
Flame Heating—Flame hardening tests were carried out on flame heating units of the Cincinnati Milling Machine Co. These units were equipped with a pyrometer to electronically control the time and surface temperature. Temperatures given in the report are those indicated by the pyrometer. The bars were rotated between the torches during heating and

quenched immediately into still oil, agitated oil at 125 F (51 C) or agitated water. The rate of heating was controlled by the spacing between tips and piece. Tests were made at indicated temperatures of 1450 F (788 C), 1500 F (815 C) and 1600 F (870 C).

Test Procedures

A longitudinal hardness survey was made on the heat treated sections to locate the point of maximum surface hardness. The bars were sectioned transversely at this point for a transverse hardness survey and macro-etching. The samples were etched with 15 per cent nitric acid to determine the case depth which was compared with the results obtained from a Superficial Rockwell and a Rockwell C survey. Two or three series of hardness tests were run in the form of skew curves extending from the surface to the interior beyond the edge of the hardened zone denoted by the macro-etch. Sections of some of the hardened cases were examined to determine the microstructures under various treatment.

The Rockwell C hardness values were plotted versus the radial distance of the penetrations from the surface. Hardness of bars from the same foundry heat, when heat treated in the same fashion, were plotted together. The hardness-depth data fell within a band 2 to 6 points Rockwell C in width and similar in shape to a hardenability band. A characteristic band is shown in Fig. 4. The two values used in presenting the data graphically are designated by (1) a vertical line made up of short dashes which indicates the range of Rockwell C hardness of the band at a depth of 0.060 in., and (2) a horizontal line made up of alternately long and short dashes which indicate the width of the hardness band at a value of 50 Rockwell C.



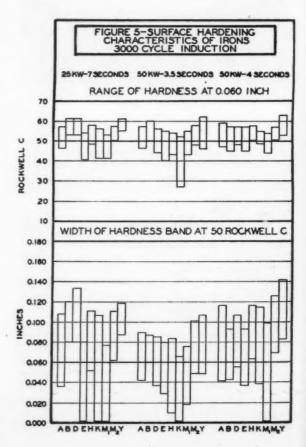
These two values were used as a means of comparing the irons when surface hardened in the same fashion. All values are plotted about the desired standards of 0.060 in. or 50 Rockwell C so it is possible to see how closely each iron meets the specifications.

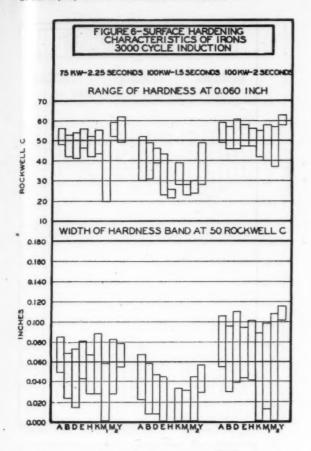
Results

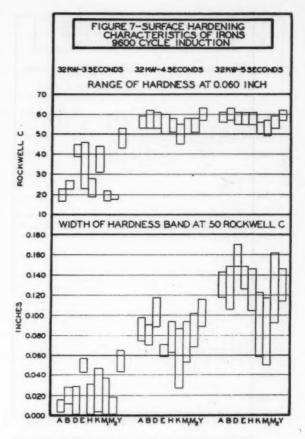
Criterion—Case hardness and depth were selected by the A.F.S. Malleable Iron Research Committee as a criterion of comparison among the various irons investigated. The limits established for the cases were 50 Rockwell C at a depth of 0.060 in. Case depths were to be kept as near this limit as possible, and the cases were not to be thinner than this. The most favorable case would then be one measuring 50 Rockwell C at 0.060 in. and dropping rapidly to the hardness of the untreated bar.

3000-Cycle Induction Hardening—Initial tests made with a multi-turn coil were considered unsatisfactory due to cases of excessive depth resulting from the prolonged heating times which were necessary. At short-time cycles the cases were non-uniform. Because of these faults no data are presented for these tests.

The nine irons were also hardened with a 150 kw, 3000-cycle motor-generator unit. A single-turn coil coupled with a perforated plate for spray quenching was used in conjunction with the unit. Results of six series of experiments are given in Fig. 5 and 6. Tests were made at 25 kw, 7 sec, and 50 kw, 3.5 and 4 sec, the results of which are given in Fig. 5. Examination of the data of the tests at 25 kw, 7 sec shows that all the irons have a maximum hardness greater than 50-Rockwell C at a depth of 0.060 in. This value corresponds to the value noted at the intersection of the upper limit of the hardness band and the vertical dashed line in the diagram in Fig. 4. The lower limit represents the minimum value in Rockwell C and the intervening range is plotted in the upper portion of the graphs shown in Fig. 5 to 14. The lower portion of these graphs show the width of the hardness band at 50 Rockwell C. This dimension is shown by







the horizontal line in Fig. 4. Examination of the 25 kw, 7 sec graph shows that in three irons, B, D, and Y the lower limit exceeds the specifications of 50 Rockwell C at 0.060 in. Irons E and M_1 are least satisfactory. The medians of the bands are definitely below the specifications. Iron K is slightly below average while irons M_2 , H, and A are better, in that order.

At 50 kw, 3.5 sec, none of the irons have lower limits exceeding the specifications, but the medians of B, Y, and M_2 are greater than the limits, while K is definitely subaverage. Increasing the heating time to 4.0 sec with the same kw raises the medians of the irons so that all, except K, exceed specifications with Y being definitely above.

Tests at higher power inputs are shown in Fig. 6. At 75 kw, 2.25 sec both M_2 and Y definitely exceed specifications, and K is far below average. No irons meet the specifications when heated at 100 kw for 1.5 sec, but at 2.0 sec all the irons are up to or close to specifications, and Y is far above the limits.

9600-Cycle Induction Hardening—The nine irons were tested under six different conditions. In Fig. 7 results are given of tests at 32 kw at 3.0, 4.0 and 5.0 sec heating time, and in Fig. 8 results are shown for 50 kw, 2.0 sec and 75 kw, 1.5 and 2.0 sec. Figure 7 illustrates clearly how an increase in heating time raises the case hardness and depth. At 32 kw, 3.0 sec none of the irons meet specifications, but Y is almost up to specifications. By increasing the heating time to 4.0 sec all the irons are above specifications; a further

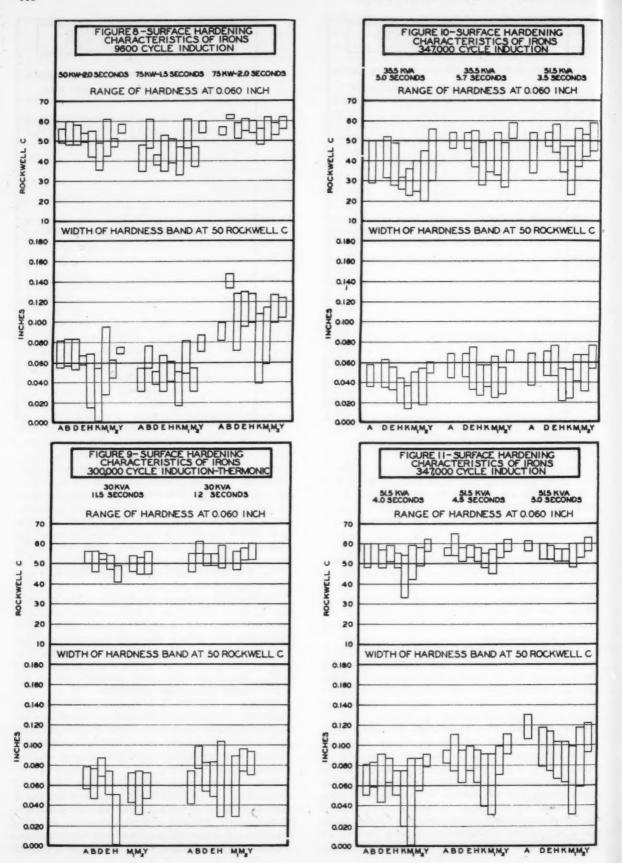
increase to 5.0 sec raises the hardness and case depth still more. A much greater spread in the hardnesses and case depths occurs at higher power inputs. At 50 kw, 2.0 sec, Y is well above average as are B and D; iron K is below average. At 75 kw, 1.5 sec only irons B, M₁ and Y are above average while at 2.0 sec all the irons are above the specifications, with B being far above the standard.

The tests were made with the same single-turn coil used with the 3000-cycle unit.

300,000-Cycle Induction Hardening—Eight irons, all but K, were heated in a 5-turn copper coil and quenched into a tank of highly agitated water. At 11.5 sec heating time, iron H was below average; the others were grouped about the median line. An increase in time to 12.0 sec raises all hardness and depths. Irons B, M_2 , and Y are well above average; and all the others exceed the median value.

300,000 to 347,000-Cycle Induction Hardening— Tests were run under eleven different conditions on the nine irons. A single-turn copper coil coupled to a perforated plate for spray quenching was used in conjunction with a rotating spindle to insure a uniform heating pattern.

Two runs were made at 35.5 kva with 5.0 and 5.7 sec heating times. Results are not given for the B iron, since some of the bars used in these tests were from a second shipment of stock bars and were below standard. At 5.0 sec all the irons were below average. The poorest were H and K. As in other tests increas-



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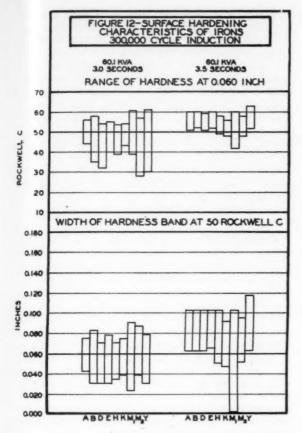
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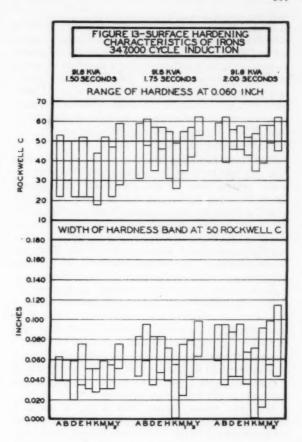


ing the heating time (to 5.7 sec) raised the median value of the hardness and depth. Iron Y is well above average and irons A and D are average. All others are still below the limits. The results of these two tests, as well as of a run at 51.5 kva, 3.5 sec, are shown in Fig. 10. Only irons E and Y are above average at the conditions of 51.5 kva, 3.5 sec. Other irons are subaverage with iron K the poorest.

Figure 11 illustrates the surface hardening characteristics of the irons with a power input of 51.5 kva at 4.0, 4.5, and 5.0 sec. At 4.0 sec only irons K and M_1 were below average. All others were definitely above specifications with Y well above the limits set. At 4.5 and 5.0 sec all irons exceed specifications. Iron Y again exceeds the specifications more than the other irons.

Results of tests made at 60.1 kva, 3.0 and 3.5 sec are given in Figure 12. At 3.0 sec the median values of the hardness bands were below 50 Rockwell C in the case of all but irons A and M₁, which were just average. The other irons were grouped about 45 Rockwell C. At 3.5 sec all irons were above average. In this test only M₁, which is average, is much below the others.

Further experiments were run at an increased power. Three tests were made at 91.6 kva using 1.5, 1.75 and 2.0 sec heating times; the results are shown in Fig. 13. No irons exceeded specifications when heated 1.5 sec; however, iron Y almost met the specifications and iron K was least satisfactory. Heating for 1.75 sec raised Y and B well above 50 Rockwell C, and E was average. Here again iron K was subaverage.



When bars were heated for 2.0 sec the case depth and hardness again increased. Only irons H, K, and M_1 were still below specifications. The other irons exceeded the specifications by 1 to 5 points Rockwell C.

Flame Hardening—Initial tests were used to determine the optimum temperature for the experiments. The most satisfactory was found to be 1500 F (815 C). Tests were made using still oil, agitated heated oil, and agitated water quenches. Neither still oil nor agitated heated oil were found to be satisfactory. Using an agitated water quench, two runs were made with torch settings of $\frac{9}{16}$ in. and $\frac{3}{4}$ in. from tips to piece. At $\frac{3}{4}$ in. irons K and M₁ are far below average, and irons B and D are slightly below the limits set. Iron Y was maximum in hardness. Irons B and Y are well above average with torch setting of $\frac{9}{16}$ in. tip to piece, and irons A, E, and M₁ were well below average; the other irons met specifications.

Discussion of Results

3000-Cycle Induction Heating—Satisfactory cases were obtained when it was possible to deliver sufficient power per unit of surface area. Insufficient power meant non-uniform cases, if the heating time was too short. At longer heating times the case became more homogeneous, but the depth of the hardened zone became excessive. When operating between 50 and 100 kw, the cases became more uniform, and differences in chemical composition or microstructure tended to become unimportant; however, heating time became critical. A difference of one-half second was sufficient

to change the irons from well below specifications to well above specifications. This was particularly apparent at 100-kw input, as is illustrated in Fig. 6. Examination of the data indicated that a structure made up of a dense pattern of small spheroids of cementite, such as in iron Y, will respond most readily to 3000-cycle heat treatment. A less dense pattern of coarse spheroids of cementite proved to be least satisfactory. Irons K and M₁ were consistently below average, and both have such a structure.

9600-Cycle Induction Heating — Results obtained with the 9600-cycle motor-generator indicate that any power greater than 32 kw, the lowest investigated. would be satisfactory for obtaining cases of 50 Rockwell C at 0.060 in. It is apparent from examination of Fig. 7 and 8 that the response of the irons at lower power per unit area was much more satisfactory than was the response with bars heated with the 3000-cycle motor-generator. The hardness bands for all the irons were narrow and all of the irons hardened to the specifications of 50 Rockwell C at 0.060 in. without excessive deviation from these specifications. Iron Y consistently hardened to a higher value, while K as consistently had the lowest hardness. That a difference in response of the irons to heat treatment occurred is more easily seen by comparing the widths of the hardness bands at 50 Rockwell C. It is obvious that iron K is least satisfactory, and irons D and Y respond most markedly at the low power input of 32 kw.

At higher power inputs irons B and Y are most affected. Iron B is made up of medium pearlite and spheroidized cementite. A coarsely spheroidized structure hardens to higher Rockwell C values, but the width of the hardness band is excessive, indicating the

nonhomogeneity of the structure.

Three comparisons can be made which are of interest. Irons A and Y have similar chemical compositions and the same history through most of the first stage malleabilization cycle; irons D and K vary somewhat in chemical composition but have the same heat treatment, the difference being that the degree of decomposition of the pearlite to spheroidized cementite is much less complete in iron D than in K; irons M1 and M2 were poured from the same furnace, but a ladle addition of manganese was made to M2, and the malleabilizing cycles differ. That iron Y responds more readily to heat treatment than iron A is obvious from the results shown in Fig. 7 and 8. Since the chemical composition of iron Y differs only in containing an additional 0.09 per cent carbon, the difference in response must be due to differences in microstructure and probably, in amount of combined carbon. This is also true in the case of irons D and K, but iron K has 0.21 per cent more carbon so the major factor must be microstructure. In all cases where iron D met or exceeded specifications, iron K was well below the hardness of the D iron at 0.060 in. or the depth of hardness at 50 Rockwell C. Irons M1 and M2 differ in microstructure and also in response to heat treatment. At 32 kw iron M₂ with a lamellar structure responds more readily to heat treatment. At higher power inputs M₁ responds more readily, but there is less uniformity in the hardened structure, and the hardness band is much wider than that of M2.

300,000-Cycle Induction Heating-Use of a multiturn coil in conjunction with a unit delivering a relatively low total power did not prove satisfactory. Since a high-frequency unit develops a field affecting only the "skin" of the metal in the field, heating below the surface must be by diffusion into the metal, and a case depth of 0.060 in. is excessive for such an arrangement. By heating for a sufficient time all the irons met specifications, but iron H responded more slowly than the other irons at 11.5 sec, then hardened to a greater extent in another half second. Results of tests when heated 11.5 and 12.0 sec in the 30 kva high-frequency induction heating unit are given in Fig. 9. Due to the excessive time for heating surface hardening with the high-frequency when power input per square inch of surface was low (1 to 2 kw) was not considered satisfactory.

5

300,000-347,000 Cycle—Coupling of a single-turn coil to a high-frequency unit capable of delivering up to 100 kva eliminated excessively long heating cycles. The single-turn coil affected a surface area of about 3 in. rather than an area in excess of 9 in., the area of the heat affected zone, when using the 5-turn coil-coupled to the thermonic unit. The high power input per square inch, possible with the small coil, insured homogeneous cases of uniform hardness at depths investigated. Results of tests made with this equipment are given in Fig. 10, 11, 12, and 13. Two variables were investigated: the effect of increasing time of heating at constant power input and the effect of increasing power input to the coil. Increasing either variable gave cases of higher hardness and greater

depth, as well as greater uniformity.

In Fig. 10 results are presented of experiments at 35.5 kva, 5.0 and 5.7 sec and 51.5 kva, 3.5 sec; 35.5 kva was the minimum power generated and represented the plate input. The actual power to the work is approximately equal to 40 per cent of the kva input. The factor 40 per cent is an average of the range (37 to 42 per cent) which has been found to be the efficiency of radio-frequency-type generators. At 5.0 sec none of the irons was satisfactory, but iron Y did harden to a maximum value greater than 50 Rockwell C. Iron D also exceeded 50 Rockwell C, but to a lesser degree. Irons H and K were barely affected. This indicates the importance of selecting the proper microstructure when it is necessary to use a low power unit for deep hardening. A structure made up of many small spheroids of cementite is most satisfactory. while a coarse pearlite, or coarse spheroid is least satisfactory. Increasing the heating time to 5.7 sec raises the hardness of the case to 0.060 in. and also increases the depth of case at 50 Rockwell C, but the same relationship observed at the shorter time of 5.0 sec is true. Coarse pearlite or spheroids of cementite still were the least satisfactory structures. The median values of H, K, M1, and M2, all of which have these microstructures, are well below the median values of the other irons. Iron Y still responds more than any of the others. The effect of an increase in the power to the coil, such as is illustrated in the graph for 51.5 kva, 3.5 sec, is quite apparent. While heated for a shorter time, the irons still attain maximum hardnesses greater than is possible at 35.5 kva although bars at

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35.5 kva were heated for an additional 2 sec. Only H and K failed to respond readily. In Fig. 11 results of heating at 51.5 kva for 4.0, 4.5, and 5.0 sec are given. The same relationship observed at 51.5 kva, 3.5 sec is retained at 4.0 sec with the values displaced upward to higher hardness values and greater case depths. This is equally true at 4.5 and 5.0 sec. At the longer heating times a mixed fine pearlite-spheroidized cementite structure (A,B) responds as well to treatment as does iron Y.

A further increase in power to 60.1 kva with heating times of 3.0 and 3.5 sec was made, and the results of these experiments are given in Fig. 12. On examination of the graphs, it is obvious that differences in hardness at 0.060 in. and the width of the hardness bands at 50 Rockwell C have been sharply decreased. The median values of the bands differ only slightly; this is also true of the maximum hardnesses. Iron M₁ responded more satisfactorily to the heat treatment than at lower power inputs, but the spread of hardness-depth values was excessive. The hardness band was nearly twice as wide as the band width of the other irons. As was the case at lower power inputs, irons H and K responded less than the other irons and iron Y to the greatest extent.

Another series of tests was run with a power input of 91.8 kva, and bars were heated for 1.50, 1.75, and 2.00 sec. Results of these experiments are given in Fig. 13. The same relationships observed in the previous tests appeared here. Irons A, B, and Y respond

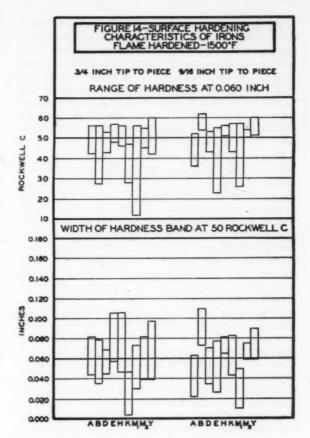


Table 2—Degree of Response of Pearlitic Malleable Irons to Surface Hardening Treatment,
Based on Weighted Averages

| | | | | | Orde | er of Response | | | | |
|--------------|-----------|-------|----------------|------------|----------------|------------------|----------------|------------------|----------------|----------------|
| Condition | 15 | 1 | 2 | 3 | 4 | 5 | 6 | 7 | 8 | 9 |
| 3000-Cycle | | | | | | | | | | |
| 25kw-7 | Sec | Y | D | В | M_3 | H | A | K | (E,M_1) | |
| 50kw-3.5 | Sec | Y | В | M_2 | A | D | M _a | E | H | K |
| 50kw-4.0 | Sec | Y | M_2 | H | D | A | K | В | E | M ₁ |
| 75kw-2.25 | Sec | Y | A | (E, M_2) | K | (B,D,H) | M_1 | | | |
| 100kw-1.5 | Sec | (A,D) | Y | В | E | (K,M_1,M_2) | H | | | |
| 100kw-2.0 | Sec | (Y,A) | D | В | E | K | (H,M_3) | M_1 | | |
| 9600-Cycle | | | | | | | , | | | |
| | Sec | Y | (E,K) | (B,D) | H | M ₁ | A | M _a | | |
| 32kw-4 | Sec | Y | D | В | A | M ₂ | (E,H) | \mathbf{M}_{1} | K | |
| 32kw-5 | Sec | (Y,D) | В | (A,E) | (H,M_2) | (K,M_1) | | | | |
| 50kw-2 | Sec | Y | В | D | A | E | M, | M_z | H | K |
| | Sec | Y | В | M_1 | H | E | (A,M_2) | D | K | |
| | Sec | В | Y | H | E | M_{a} | M ₁ | (D,A) | K | |
| 300,000-Cyc | le | | | | | - | | | | |
| | Sec | D | A | В | (E,Y) | M, | M_2 | H | | |
| 12.0 | Sec | В | (M_2,Y) | (D,E,H) | M, | A | | | | |
| 300,000-347, | 000-Cycle | | , , | , | - | | | | | |
| 35.5kva-5.0 | | Y | D | A | E | M ₂ | M ₁ | H | K | |
| 35.5kva-5.7 | Sec | Y | A | D | E | K | M, | н . | M_a | |
| 51.5kva-3.5 | Sec | Y | E | D | \mathbf{M}_1 | M ₂ | A | H | K | |
| 51.5kva-4.0 | Sec | Y | E | B | A | M ₂ | D | H | K | M, |
| 51.5kva-4.5 | Sec | (Y,B) | (A,E) | M_a | D | H | K | M_1 | | |
| 51.5kva-5.0 | Sec | (Y,A) | Ma | D | E | H | K | M ₁ | | |
| 60.1kva-3.0 | Sec | A | M_1 | K | В | (E,M_a) | (H.Y) | D | | |
| 60.1kva-3.5 | Sec | Y | E | В | A | D | H | M_a | K | M ₃ |
| 91.8kva-1.5 | 0 Sec | Y | (A,B,E,M_1) | M_3 | D | H | K | | | |
| 91.8kva-1.7 | 5 Sec | Y | В | E | M_z | · A | D | H | M_1 | K |
| 91.8kva-2.0 | | (A,Y) | M _e | E | D | M_1 | В | H | K | |
| Flame Hard | | / | | | | | | | | |
| %6-in. tip-p | | В | Y | H | M _e | K | D | (A,E) | M ₁ | |
| 4-in. tip-pi | | E | (H,Y) | (A,M_2) | (B,D) | \mathbf{M}_{1} | K | | | |

to the greatest extent, and irons H and K, the least. Only a slight increase in heating time is sufficient to change the results markedly. An increase in time from 1.50 to 1.75 may raise the hardness at 0.060 in. more than 10 points Rockwell C and nearly double the depth of the effective case. It is apparent, then, that heating times must be closely controlled, or irons responding less readily to heating should be used. In such an event, irons H and K might prove satisfactory, since they respond more slowly.

Flame Hardening-Results of experiments conducted at the Cincinnati Milling Machine Company appear in Fig. 14. Tests were conducted on a flame hardening machine using oxygen and acetylene, and equipped with a rayotube pyrometer for control of the surface temperature. The depth and hardness of the case was fixed by the distance separating the test bar and the torch heads. The flame used was slightly oxidizing. Since the pyrometer controlled the torches electronically, all tests supposedly were run to the same surface temperature. An examination of the results in Fig. 14, when the setting used was 3/4-in. from tip to piece, indicates that iron Y again had the maximum hardness, and iron K was least satisfactory. It is interesting to note that both H and M2, normally quite unresponsive, were satisfactory. This deviation from normal response is undoubtedly due to longer heating times. Both irons were heated as much as 2 sec longer, evidently due either to a different surface emissivity or to the fact that the irons required a longer time to come to 1500 F with a resultant deeper case. This may be due to their microstructures or to chemical composition. Both irons were high in manganese, which might be responsible.

Another experiment was conducted with the torch setting changed from 3/4 to 9/16 in. tip to piece. In this case irons B and Y responded most markedly. At this setting the heating times were shorter, and results are comparable to those observed at higher power input to the high or low frequency induction units where the two structures responding to the greatest extent were fine pearlite-spheroidized cementite or a dense pattern of small spheroids of cementite such as in irons B and Y. The shorter distance from tip to piece corresponds to an increase in power or energy per unit area. Iron K appeared to give abnormal results, since both hardness and depth were higher than expected. However, the average heating time for iron K was above the average for the other irons and the deviation from the norm may be due to the longer heating time. The lower values of iron M1 are also believed to be due to the heating time since the cycle for M1 ran more than a second below the average for the other irons.

Comparison of Flame and Induction Hardening—Response to the various irons to different treatments has been discussed on the basis of the specific conditions of one particular experiment at a certain power and frequency, or torch setting. In Table 2, the results are combined so that it is possible to compare all conditions, and to observe whether the irons tend to respond similarly in all cases. In this fashion it is possible to determine if some optimum microstructure exists as well as to determine if certain microstructures

are unsatisfactory. In order to obtain the results appearing in Table 2, a weighted average was used. The median values of the hardness bands at 50 Rockwell C and at 0.060 in. were determined and the maximum median values of both Rockwell C and depth in thousandths of an inch were given a value of 1, the next 2 and so on down to 9 for the minimum median values. The two values for each iron were then added and the code identifications of the irons were arranged so that the one with the lowest number was first and so on down to the last, which would have the largest number, for example, Y, B, D, A, E, M2, H, M1, K. In this fashion both variables were considered, so that an iron with a high hardness but too thin a case would not be considered best, simply on the basis of the hardness alone. Brackets have been used to indicate that the irons had the same numerical values.

It is apparent on examination of Table 2 that iron Y responded most readily. Iron Y fell below second place in only three instances out of 27 different experimental conditions. Irons B and A also were quite satisfactory. E and D were only slightly less satisfactory, followed by M2, and finally irons H, M1 and K, in that order, which were unresponsive. K was rarely satisfactory. The differences in the irons is apparent when the weighted averages are summed up, and the comparison is made on a numerical basis. The difference between any two numbers indicates the over-all difference in response of those two irons under all conditions. This numerical comparison is given in Table 3. The response of iron Y and the lack of response of iron K to all treatments is obvious on examination of these values.

TABLE 3—COMPARISON OF PEARLITIC MALLEABLE IRONS
RESPONSE TO ALL CONDITIONS INVESTIGATED, BASED
ON A SUMMED WEIGHTED AVERAGE

| Iron | Summed Average |
|------------------|----------------|
| A | 225 |
| В | 159* |
| D | 241 |
| E | 239 |
| H | 336 |
| K | 371** |
| \mathbf{M}_{1} | 374 |
| M_2 | 280 |
| Y | 96 |

 Does not include four tests due to faulty bars so value probably should be approximately 180.

** Does not include two tests, so value probably should be approximately 400.

Conclusions

The following conclusions have been reached, considering induction hardening and flame hardening separately and noting those factors common to both methods of surface hardening.

Induction Hardening

 Irons having structures of pearlite, mixed pearlite and spheroidized cementite, spheroidized cementite, or a ferritic matrix containing chains of iron carbide at the grain boundaries may be hardened satisfactorily under properly selected conditions. an sa ga 34

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Cor 1. Chemical composition within the limits investigated has no noticeable effect on the response of the

irons to hardening.

3. Proper time and powers were controlling factors in fixing case depth and hardness. When time and power were properly selected, satisfactory results were obtained with all irons tested.

4. A case depth of 0.060 in. with a Rockwell C 50 could be obtained with a 9600-cycle operating between 32 and 75 kw, a 3000-cycle unit operating between 25 and 100 kw, and a 300,000 to 347,000-cycle unit operating between 35 and 92 kva, when using a single-turn coil.

5. An increase in heating time or in power input produced higher case hardnesses, deeper cases, and a

more homogeneous microstructure.

6. A microstructure consisting of a dense pattern of finely spheroidized cementite consistently yielded the highest hardnesses at 0.060 in. and maximum depth of case at a value of 50 Rockwell C over the range of power and time investigated at frequencies of 3000, 9600, and 300,000 to 347,000 cycles.

7. A microstructure consisting of a matrix of ferrite and coarsely spheroidized cementite proved to be least satisfactory over the range of power and time investigated at frequencies of 3000, 9600, and 300,000 to

347,000 cycles.

Flame Hardening

1. A comparison of irons on the basis of chemical composition indicated an increase in the manganese content was accompanied by an increase in hardness and depth of effective case in irons of similar lamellar microstructure.

2. All irons were hardened satisfactorily under selected conditions. Variations from these conditions

produced variations among the irons.

3. Temperature control is critical, because of the necessity for adequate homogenization of the austenite, which requires a certain minimum temperature, and the danger of fusion, which establishes a maximum possible temperature.

4. An increase in heating time produces a more uni-

form and deeper hardened case.

5. To meet the specifications of 50 Rockwell C at a depth of 0.060 in. of water quench is necessary. A still oil quench and a violently agitated heated oil quench were both unsatisfactory for the irons investigated.

6. A microstructure consisting of a dense pattern of finely spheroidized cementite proved to be consistently most satisfactory under all conditions investi-

gated.

7. A microstructure made up principally or completely of coarse spheroids of cementite proved least satisfactory under the conditions investigated.

Common Factors

1. A microstructure consisting of a dense pattern of

finely spheroidized cementite proved most satisfactory under a majority of the conditions investigated.

A microstructure made up of a ferrite matrix and coarsely spheroidized cementite proved least satisfactory under a majority of the conditions investigated.

3. A bullseye structure did not appreciably effect

uniformity of the hardness of the cases.

4. An increase in heating time under the same conditions produced higher case hardness, a deeper case, and a more homogeneous microstructure.

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4. J. Bates, J. Betts, R. Bush, B. Davidson, R. Eichbauer, K. Packer, C. Perry, C. Rudy, G. Sloane, L. Stair, and W. Steupker, Engineering students of the University of Michigan, who assisted in making mechanical tests and photomicrographs.

DISCUSSION

Chairman: F. W. JACOBS, Lake City Malleable Iron Co., Ashtabula, Ohio.

Co-Chairman: R. P. SCHAUSS, Illinois Clay Products Co., Chicago.

MEMBER: Were all these hardness readings taken on Rockwell

MR. Bush: The hardness readings were Rockwell C or Superficial Rockwell converted to Rockwell C.

MEMBER: Did you have any difficulty taking Rockwell readings? MR. Bush: On straight Rockwell C, near the sample edge, we encountered some difficulty, but by the use of Superficial Rockwell we managed to eliminate it.

MEMBER: Is there any correlation between these hardnesses

you obtained?

Mr. Bush: The hardenability of the irons was so high that it was not possible to obtain a satisfactory correlation based on Jominy test bar results. Not all of the irons were tested in this fashion so it is possible that some would show a lower hardenability allowing some comparison on this basis. The only other comparison possible is that indicated in the graphs.

MELT QUALITY AND FRACTURE CHARACTERISTICS OF 85-5-5-5 RED BRASS

By

F. M. Baker, C. Upthegrove, and F. B. Rote*

ABSTRACT

This is the second progress report on the study of melt quality and fracture characteristics of 85-5-5-5 Brass, a research project sponsored by the Research Committee of the Brass and Bronze Division of the American Foundrymen's Society at the University of Michigan. In the first report it was shown that the fracture characteristics of tensile bars and of separately cast flats up to 2 in thick can be correlated with melt quality. The present report presents the data obtained in the search for a more rapid method of accurately evaluating the melt quality of the 85-5-5-5 alloy on the basis of the fracture characteristics.

The melt quality is determined by examination of the fracture of a $\frac{1}{2}(x \times 3 \times 6)$ -in. block cast in core sand and chilled against cast iron on a $\frac{1}{2}(x \times 6)$ -in. face. Characteristic fracture appearances indicate the melt quality and, if the quality is low, the probable cause. A complete test, including examination of the fracture, requires approximately 3 min.

In a previous paper¹ by John F. Ewing, C. Upthegrove, and F. B. Rote, the results of a study of the influence of variables in the melting procedure on the melt quality of red brass and the interrelation of melt quality and fracture characteristics in sand cast plates ½, 1, and 2 in. thick were presented. It was shown that a definite relationship did exist between melt quality and fractures, and that the mechanical properties which could be expected in sand castings were predictable from fracture observation. The investigation on which the above paper was based was sponsored by the Research Committee of the Brass and Bronze Division of the American Foundrymen's Society, as is the investigation upon which the present paper is based.

In the present investigation, attempts are being made to develop a fracture procedure which will permit more rapid appraisal of the melt quality in red brass than was possible when the sand cast plates were used as quality indicators. One of the most time-consuming factors in the use of the plate castings is the time required for solidification, cooling, notching, and fracturing the heavy sections required for proper quality determination. Because of this, it was decided

to investigate other types of castings which could be analyzed in shorter time periods than was required by the plates. The most successful casting is a 3/4 x 3 x 6-in. chilled block, which permits very rapid determination of melt quality and produces reliable fracture indications.

Data presented in the succeeding sections of this report were secured from a total of 55 melts made in the induction or gas-fired furnaces. Wide variations in melting practice were employed to obtain information on the possibilities in the use of the chilled blocks for quality determination under all possible conditions which might be encountered in brass foundries.

Standards of Melt Quality

The term "high melt quality" has come to mean, "melts which are so low in deleterious gas contents and other impurities that the metal, as it is poured from the pot, is capable of producing superior castings, if the molding sand and gating technique be correct." It is necessary, therefore, that a quality test be one which will evaluate a metal with respect to gas content.

In this investigation, melt quality standards have been based on the tensile properties and density of tensile test bars made according to ASTM specification B208-46T, Fig. 3. Melt quality, so established, is correlated with the fracture appearance of sand cast plates of the types referred to above and of chilled blocks made in baked core molds and cast against a gray iron chill plate.

Experimental Procedure

The chilled casting used for fracture observation is a block $3/4 \times 3 \times 6$ in., which is chilled on a $3/4 \times 6$ -in. face. The chiller is a cast iron plate $11/4 \times 51/4 \times 12$ in. The casting is made in a core with outside dimensions of $3 \times 4 \times 93/4$ in., which is made of a standard core mix of lake sand (A.F.S. 50 grain size) bonded with a 1.0 per cent oil, 1.0 per cent bentonite, and 1.0 per cent cereal binder. The cores are baked either 21/2 hr at 425 F or 16 hr at 375 F, depending on the cycle required by other cores produced at the same time in the University of Michigan foundry. The metal is poured

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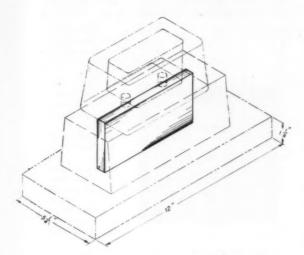
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Personnel of the Research Committee of the Brass and Bronze Division sponsoring this research project are Dr. B. M. Loring, Chairman, A. H. Hesse, Gordon J. LeBrasse, Clausen A. Robeck and Dr. A. J. Smith.

into a basin $1\frac{1}{2} \times 3\frac{7}{8} \times 2\frac{1}{2}$ in. deep and fed into the block cavity through two $\frac{1}{2}$ -in. diameter openings. The basin is kept full during pouring to obtain a constant, rapid pouring rate. Figure 1 shows the assembled chill block core and chiller plate.



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Fig. 1-Assembly of Chill Block Core and Chill Plate

Metal for investigation in this project was melted in two types of furnaces: a gas-fired crucible furnace which was operated with controlled oxidizing or reducing atmospheres, and a 3,000-cycle induction furnace. The crucibles used in the gas furnace were the clay-graphite type. The induction furnace was lined with a magnesia ramming refractory. The charges used consisted of 50 per cent ingot metal and 50 per cent cast scrap. Melts of about 100 lb were made in gas-fired furnace and of 200 lb in electric furnace.

In each melt a chill block was poured at the peak superheating temperature, the metal was permitted to cool to the selected pouring temperature, 2150 F, for most melts, and the remaining castings were poured in the following order: chill block, sand cast plates, tensile test bar mold, and a final chill block.

The tensile test bars were then weighed for density measurement and tested. The fracture test blocks were notched at a distance of 2 in. from the end and fractured for observation.

Typical Fracture Appearances

The fractures of the chilled castings varied widely from the chilled face to the more slowly, air-cooled face. At the chilled face, the fracture was characteristically fine grained and had a blue-gray color. There was a superimposed columnar pattern which originated at the chiller and proceeded into the block to varying depths, depending on melt quality. In all blocks, the blue-gray, fine grained fracture was more extensive than the columnar pattern, and in the same manner as the columnar, varied with melt quality. The higher the melt quality, as indicated by the properties of tensile test bars, the greater is the extent of blue-gray and columnar pattern.

In the portion of the block most distant from the cast iron chiller the fracture was granular and had a reddish color for melts of high quality. In intermediate or low quality melts the fracture above the blue-gray area was mottled, with red-brown or yellowish spots in the reddish granular fracture.

Each of these characteristics has been found in the sand-cast plates, but the delineation between various fracture types was not clearly marked. In the chilled block each characteristic was accentuated by the wide variations in cooling rate produced by the chill plate.

Figure 2 shows comparative fractures of a chilled block and sand-cast 1/2, 1, and 2-in. plates. The fractures shown in this photograph are from castings made from a melt of low quality (melt 43). It will be noted that the sand cast fractures have a faint gray rim with the center blue or reddish gray in 1/2 and 1-in. sections, and in the 2-in. bar, the reddish gray changes to light brown at the center. In chill block a strong blue-gray appears at chill face and extends upward with sharp division between blue-gray and light brown coloring.

Melt Quality and Fracture Appearances

As noted above, there is a clear relationship between the fracture appearance and the melt quality of red brass. For the purpose of discussion, it is desirable that melt quality be classified according to tensile properties and density of tensile test specimens. In the present investigation, the melt quality has been arbitrarily classified into three levels. These are high, intermediate, and low. High quality indicates a tensile strength of 37,000 psi minimum, 30 per cent clongation minimum, and a density of 8.8 or above. Intermediate quality indicates a tensile strength of 35,000 to 37,000 psi, 25 to 30 per cent elongation, and a density of 8.65 to 8.8. Melts which produce castings with properties lower than these were considered low quality.

High Quality Melts

The fractures of chill blocks made from high quality melts show the characteristic blue-gray fracture to a depth of at least 1¾ in. from the chill face, with a few of the fractures of the highest tensile properties melts having a blue-gray appearance the full depth of the block. Interspersed with the blue-gray fracture and near the chilled face are the characteristic columnar patterns, which extend at least ½ in. and much as 1½ in. into the casting from the chill face. It has been observed that columnar pattern is a function of the pouring temperature, with high columnarity above 2150 F and little or no columnarity below 2000 F. In the temperature range between 2000 to 2150 F, the columnar pattern may be used for a qualitative measure of melt quality.

The fracture beyond the columnar pattern is moderately fine grain, with the upper region above the blue-gray tending towards a gold color, up to the aircooled face. The overall color of the fracture is a distinct blue-gray tint. A typical fracture is shown in specimen three of Fig. 3. This block was from an induction furnace melt, test bars from which had a tensile strength of 40,600 psi, elongation of 34.5 per cent in a 2-in. gage length, and density of 8.9.

TABLE 1-MECHANICAL PROPERTIES AND FRACTURE CHARACTERISTICS OF 85-5-5-5 BRASS MELTS

| | | | | Tensile B | lar Prop | erties | | | Frac | ture | Characte | eristics | | |
|----------|--------------------|--------------------|--------------|-------------------------------|-------------------------------|---------|------------------------|-----------------------------------|-------------------|------|--------------------------|------------------------|--------------------|---|
| Melt No. | Melting Furnace | Pouring Temp, I | , | Tensile Strength, psi % | Elon- gation % in 2 in. | Density | Depth Blue- Gray | Grain Size* Blue- Gray C | Depth Columnar | | Oxide Inclu- sions | Degree of Mottle | Color of | Grain Size*** Upper part of Bar |
| 22B | Gas | 2150 | High | 38,600 | 31.5 | 8.75 | 2 | 1 | 11/2 | 3 | Y-1 (2) | 3 | Gold-Yellow | N 2 : |
| 29A | Induction | 2150 | High | 38,300 | 31 | 8.80 | 2 | 1 | 17/16 | 2 | Y-1 | 2 | Gold-Blue | 3 |
| 29B | Induction | 2050 | High | 38,100 | 32 | 8.84 | 15% | 2 | 1/8 | 1 | Y-2 | 1 | Blue-Yellow | / 2 |
| 30A | Induction | 2080 | High | 39,700 | 37 | 8.87 | 21/6 | 3 | 1/2 | 1 | Y-1 | 1 | Blue-Gold | 2 |
| 35A | Induction | 2140 | High | 40,600 | 34.5 | 8.90 | 29/16 | . 1 | 11/16 | 1 | Y-2 | | _ | 1 |
| 1) 37A | Gas | 2140 | High | 37,900 | 35.5 | 8.88 | 21/2 | 3 | 7/16 | 1 | Y-3 | 1 | Blue-Gold | 3 |
| 46A | Induction | 2160 | High | 37,700 | 31.0 | 8.84 | 134 | 1 | 1346 | 3 | Y-1 | 2 | Gold-Blue | 3 |
| 48A | Induction | 2040 | High | 38,400 | 30 | 8.9 | 2 | 3 | 5/8 | 1 | Y-1 | 1 | Blue-Gold | 3 |
| 49A | Induction | 2125 | High | 37,100 | 31.5 | 8.85 | 115/16 | 2 | 1 | 2 | Y-1 | 2 | Gold-Blue | 2 |
| 53A | Gas | 2100 | High | 38,100 | 30 | 8.87 | 27/8 | 2 | 11/16 | 1 | Y-1 | _ | | 2 |
| 31A | Induction | 2150 | Intermediate | 35,400 | 25.5 | 8.77 | 11/2 | 1 | 11/8 | 2 | Y-1 | 3 | Gold-Silver | 2 |
| 38B | Induction | 2140 | Intermediate | 36,000 | 27.5 | 8.82 | 2 | 3 | 13/4 | 1 | Y-1 | 1 | Blue-Gold | 1 |
| 44B | Induction | 2000 | Intermediate | 37,200 | 25.5 | 8.96 | 115/16 | 3 | 1/8 | 1 | Y-3 | _ | _ | 3 |
| 47C | Induction | 1950 | Intermediate | 35,600 | 26.5 | 8.78 | 13/4 | 2 | 7/8 | 2 | Y-1 | 1 | Blue-Gold | 1 |
| 54-4 | Gas | 2160 | Intermediate | 35,300 | 25.5 | 8.78 | 134 | 2 | 11/2 | 3 | Y-1 | 3 | Gold-Silver | 3 |
| 26A | Induction | 2150 | Low | 37,600 | 22.5 | 8.85 | 134 | 4 | 7/16 | 1 | Y-1 | 1 | Blue-Gold | 2 |
| 32B | Gas | 2100 | Low | 34,500 | 20 | 8.65 | 11/2 | 3 | 9/16 | 2 | Y-1 | 1 | Blue-Gold | 3 |
| 34A | Induction | 2250 | Low | 25,900 | 14 | 8.46 | 1/2 | 3 | _ | 2 | Y-1 | 3 | Gold-Blue | 4 |
| 42A | Gas | 2140 | Low | 34,500 | 24.5 | 8.67 | 7/8 | 4 | 1/2 | 2 | Y-1 | 3 | ·Gold-Blue | 4 |
| 45-10 | Induction | 2100 | Low | 37,000 | 24.5 | 8.86 | 13/8 | 4 | 3/8 | 1 | Y-1 | 3 | Gold-Blue | 4 |

Intermediate Quality Melts

The fracture test block from melts of intermediate quality had the same general characteristics as observed in blocks from high quality melts. However, the extent of the blue-gray area and the height of the columnar pattern which indicate high quality were definitely less in these blocks. Melts of intermediate quality had somewhat coarser fractures than those of high quality melts, and the fracture had a more reddish hue, as compared with a distinctly blue-gray color in high quality metal. In the area above the blue-gray, a lightly mottled appearance is usually observed, indicating a higher gas content in the intermediate quality melts.

The mottled appearance is more pronounced at the lower limit of elongation, namely 25 per cent, and becomes fainter as the quality improves towards the 30 per cent limit for this melt quality. A typical fracture of a melt of intermediate quality is shown in Specimen 2 of Fig. 3. This bar was poured from a melt made in the induction furnace, and held at the superheat temperature of 2300 F for several minutes. The tensile strength on this melt was 36,000 psi, elongation was 27.5 per cent, and density was 8.82.

Low Quality Melts

Low melt quality was indicated by a limited extent of blue-gray fracture, and, in blocks from the lowest quality melts, by a very rough and mottled appearance. In the blue-gray region there is frequently this superimposed mottled pattern• which correlates with the lowest quality in the metal. The general appearance of the fracture of low quality melts in much coarser and rougher than the higher quality melts. Certain fracture characteristics were also observed which were

indications of the reason for the low melt quality. Melts superheated to above 2350 F in the induction furnace or made under a reducing atmosphere in the gas-fired crucible furnace showed a characteristic golden colored, coarse mottled fracture. Melts which were of low melt quality because of late contact of the metal with a source of water vapor, showed a characteristic appearance, with light gold mottle in a dark brown background. A typical fracture of a low quality melt made under reducing conditions in the gas furnace is shown by Specimen 1 in Fig. 3. The tensile bar from this heat had a strength of 33,000 psi, elongation of 20 per cent, and density of 8.56.

Table 1 shows the results obtained in tensile tests, density determinations, and fracture observations of a number of melts made for investigation in this project.

Melting Practice and Fracture Appearance

The fracture characteristics detailed above were obtained in metal melted in each of the furnaces, and were influenced by the melting practice rather than by melting furnace employed. In other words, high, intermediate, and low melt quality are characterized by certain fractures, regardless of the melting unit used. Typical fractures from six blocks made under each of six melting procedures are shown in Fig. 4. The properties of corresponding test bars are shown in Table 2.

Specimen 1 was made from melt No. 49 which was melted in the induction furnace in a magnesite crucible, superheated to 2300 F, tapped into a preheated clay-graphite crucible to which 1 lb of zinc and 2 oz of 15 per cent phosphorus copper-phosphorus was added per 100 lb of metal, and poured at 2150 F. This practice has been found to produce best melt quality.

MELT QUALITY AND FRACTURE CHARACTERISTICS OF 85-5-5-5 RED BRASS



| | 1 | 2 | 3 |
|--------------------|-------------|----------------------|--------------|
| Melt No. | 43 | 38 . | 35 |
| Furnace | Gas | Induction | Induction |
| T.S., psi | 33,000 | 36,000 | 40,600 |
| Elong., % | 20.0 | 2.75 | 34.5 |
| Density Quality | 8.56 Low | 8.82 Intermediate | 8.90 High |

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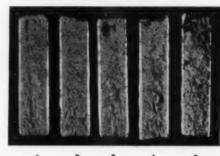
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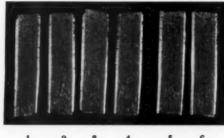
was cruated oz of lded oracFig. 3-Typical Fractures of Chilled Blocks from Melts of Varying Quality



1 2 3 4 5 Pouring Temp., F 2175 2100 2030 1975 1940

Induction Melt No. 46—High Quality

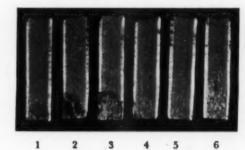
Fig. 6—Influence of Pouring Temperature on Fracture Appearance of
Chilled Blocks



| | 1 | 2 | 3 | 4 | 5 | 6 |
|----------------|---|---|---|---|---|---|
| No. of | _ | _ | | - | | - |
| ¼-in. Holes | 1 | 2 | 4 | 5 | 3 | 6 |

Gas Furnace Melt No. 54—Intermediate Quality

Fig. 7—Influence of Pouring Rate on Fracture Appearance of Chilled Blocks

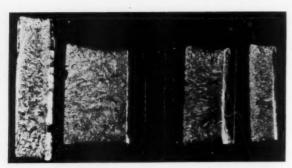


| | - | - | _ | _ | _ | _ | |
|--------------|------|------|------|------|-----|------------|--|
| Time, Min | | 2 | 3 | 4 | 5 | 30 | |
| Temp | 1600 | 1360 | 1200 | 1070 | 980 | Room Temp. | |
| | 1000 | 1300 | 1200 | 10/0 | 300 | Room remp. | |

Gas Furnace Melt No. 55—Intermediate Quality (Refer to Table 4)

Fig. 8—Influence of Quenching Temperature on Fracture Appearance of Chilled Blocks

MELT QUALITY AND FRACTURE CHARACTERISTICS OF 85-5-5-5 RED BRASS



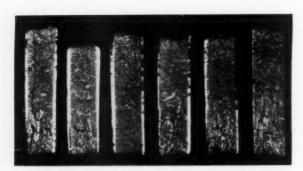
1 2 3 4
Chill Block Flat Flat Flat
3/4x3x6 in. 2x2x6 in. 1x2x6 in. 1/2x2x6 in.
(Gas Furnace Melt No. 43—Low Quality)

Fig. 2—Comparative fractures of Chilled Blocks and Sand Cast Flats.



1 2 3 3/4x3x6 ½x3x6 ½x2x6 (Gas Furnace Melt No. 43—Low Quality)

Fig. 5—Comparative Fracture Appearances of Chilled Bars Having Different As-Cast Dimensions.



Induction Induction Induction Gas-Fired Gas-Fired High Intermediate Low Quality Quality Quality Quality Quality Quality Quality Quality Fig. 4—Influence of Melting Practice on Fracture Appearance of Chilled Blocks. See Table 2 for Properties

of Test Bars and Melting Practices Employed.



Cast Solid Notched
Saw Blade
(Gas Furnace Melt No. 55—Intermediate Quality)

Fig. 9-Comparative Fractures of Chilled Bars Cast Solid and As-Notched with Steel Strip.

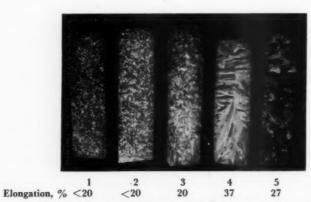


Fig. 10-Macro-etched Sections of Chilled Block Castings.

TABLE 2-PROPERTIES OF TEST BARS MADE UNDER VARYING MELTING PROCEDURES

| | | Tensile 1 | Elongati | ion | |
|-------------|-----------|--------------------|--------------|---------|-----------------|
| Melt No. | Furnace | Strength, (psi) | (% in 2 in.) | Density | Melt Quality |
| 49 | Induction | 37,100 | 31.5 | 8.85 | High |
| 31A | Induction | 35,400 | 25.5 | 8.77 | Intermediate |
| 34A | Induction | 25,900 | 14.0 | 8.46 | Low |
| 53A | Gas | 38,100 | 30.0 | 8.87 | High |
| 52 | Gas | 35,400 | ,25.0 | 8.8 | Intermediate |
| 27A | Gas | 34,500 | 14.5 | 8.52 | Low |

Melting Practice:

Melt 49 -Heat to 2300 F; pour 2150-2125 F.

Melt 31A-Heat to 2350-2375 F; pour at 2150-2125 F.

Melt 34 -Heat to above 2400 F; pour at 2150-2125 F.

Melt 53A-Oxidizing atmosphere; heat to 2250 F; degas 10 min with dry nitrogen; pour 2125-2100 F.

Melt 52 -Neutral atmosphere; heat to 2250 F; pour at 2150-2125 F.

Melt 27 -Reducing atmosphere; heat to 2200 F; pour at 2150-

Specimen No. 2 was made from Melt No. 31A which was produced under essentially the same practice as melt No. 49, except that the metal was superheated to 2350 to 2375 F, and poured at 2150 F. The higher superheat temperature, with attendant longer time at high temperature, permitted the metal to dissolve gas from the atmosphere and decreased the properties to the intermediate level. The levels of blue-gray and columnar fractures are somewhat lower than in melt

Specimen No. 3 was also from an induction furnace melt, No. 34A, which was heated to a temperature even higher than melt No. 31A, and was held for several minutes at this temperature. The thermocouple used for measurement of temperature failed, so that the actual temperature was not known. The metal became badly gassed, and the fracture shows little bluegray and columnar structures and much mottle, which indicates low melt quality.

Melts 53A, 52, and 27A were made in the gas-fired furnace under varied procedures to produce different melt qualities. Melt 53A was melted under an oxidizing atmosphere, superheated to 2250 F, degassed 10 min with dry nitrogen, and poured at 2125 F. This produced high melt quality, as evidenced by a tensile strength of 38,100 psi and 30 per cent elongation. The fracture consisted of almost complete fine, blue-gray background with %16-in. columnar. Melt 52 was

TABLE 3-CORE MIXES USED TO INVESTIGATE INFLUENCE OF BINDERS ON FRACTURE OF CHILLED BLOCK

| | | | Per | Cent Addit | ion |
|---------|----|--------|-------|------------|-----------|
| Mix No. | | Sand ' | Oil | Cereal | Bentonite |
| 1 | | Lake | 1 | 1 | -1 |
| 2 | | Lake | 2 | 1 | 1 |
| 3 | | Lake | 2.5 | 1 | 1 |
| 4 | 50 | Lake | 1.5 | 1 | 1 |
| | 50 | Bank | 41.30 | | |
| 5 | | Lake | 1 | 1 | 1 |
| 6 | | Lake | 1.5 | 2 | 1 |

Lake Sand-A.F.S. 50 grain size Bank Sand-A.F.S. 95 grain size.

melted under an essentially neutral atmosphere and was of intermediate quality. This was indicated by lower blue-gray and columnar structure than in the bar from heat 53A. The upper portion of this bar was somewhat mottled. Melt 27A was made in the gas-fired furnace under a reducing atmosphere. The tensile properties were very low; 34,500 psi tensile strength and 14.5 per cent elongation. The fracture of the chill blocks shows a high columnar pattern due to a pouring temperature of 2225 F. It was badly mottled with light, yellow-gold facets in a reddish background. Fractures of this type indicate very low melt quality.

Factors Other Than Melt Quality

In order to establish a standard testing procedure for chilled blocks, such that the fracture appearance would be a true indication of melt quality, an investigation has been made to determine the influence of production variables on fracture appearance. Those variables which have been studied to date include: chilled block dimensions, core mold composition, pouring temperature, pouring rate, shake-out or quenching temperature, and prior notching to facilitate rapid fracture for examination. On the basis of these investigations, a standard procedure has been developed.

Chill Block Dimension

The fracture characteristics observed along the face of a chill block from metal of a certain quality are due to variations in cooling rate induced by the chill plate, which, it was felt might introduce a possible effect of chill block size on fracture appearance. To investigate this factor, a series of blocks of different sizes was poured from each of several heats of varied melt quality and fractured for examination. The sizes were $\frac{3}{4} \times 3 \times 6$ (standard), $\frac{1}{2} \times 3 \times 6$, and $\frac{1}{2} \times 2 \times 6$ in. Typical fractures of blocks of the three sizes poured from melt 43 (low quality, gas-fired furnace melt) are shown in Fig. 5. Each block shows the characteristic low blue-gray and columnar depths, with considerable mottle in the upper sections.

Any of the blocks could be used as an indication of poor melt quality, since the depth of blue-gray is limited; however, the 2-in. block would be inadequate for differentiation between intermediate and high quality. Further, a 1/2-in. width provides little area for examination. Therefore, the 3/4 x 3 x 6-in. casting is preferred and is proposed as the desirable block for

fracture test quality control.

Influence of Core Mold Composition

Since red brass will pick up reducing gasses quite readily, a study was made of core mold composition, to determine whether an unsatisfactory core mixture might influence the fracture obtained. In this phase of the investigation a total of six different core mixtures, varying in type and grain size of the sand used and in the quantity of binders were prepared. It was apparent from the appearance of the chill block fractures that the core composition had little significant effect, in that all blocks appeared practically identical. Gore mixes investigated are summarized in Table 3. The mixture used for Specimen 1, lake sand (A.F.S.

50 grain size) with 1 per cent cereal, 1 per cent bentonite and 1 per cent oil was used in all other cores throughout this investigation.

Influence of Pouring Temperature

Since pouring temperature is a probable variable of major magnitude in foundry quality checks, a study was made of the influence of pouring temperature on the fracture appearance of the chilled blocks. Five melts have been run for this purpose. The fractures of bars made from a typical melt, No. 46, are shown in Fig. 6. The pouring temperatures varied from 2175 F in Specimen 1 to 1940 F in Specimen 5. It is shown that the pouring temperature has little effect on the fracture, except in the last chill block, which was poured at 1940 F. The bars poured at temperature above 1940 F showed the characteristic columnar and blue-gray fracture, while the bar poured at 1940 F had a rather coarse granular fracture, which indicated almost complete instantaneous solidification in the mold. Similar fractures were observed in bars poured at low temperatures from the other melts. Because of these results, it appears desirable that the pouring temperature of fracture test castings be held within the range of 2200 to 2000 F.

Influence of Pouring Rate

Early experience with the chilled casting indicated that the rate of pouring could exert a strong influence on the fracture appearance. To investigate the pouring rate limits, several melts of varying quality have been made and poured through baked core basins having from 1 to 6, ½-in. holes for admitting metal to the mold. A set of typical fractures from melt No. 54 (intermediate quality—gas fired furnace) is shown in Fig. 7. The four blocks on the left were poured through basins having one, two, four and five holes, respectively, while the two blocks on the right were poured through basins having three and six holes, respectively.

It will be noted that of the four blocks, the last three have identical fractures, indicating that pouring at rates permitted by two, four, or five holes will produce representative fractures. However, the first block (one hole basin) shows the coarse granular fracture typical also of cold metal. It appears, therefore, that the pouring rate is not a factor in fracture appearance, unless very slow rates are used. No upper limit need be set. It is recommended, however, that the rate produced by four holes 1/4 in. or 21/2 in. be set as a standard. This permits pouring in about 11/2 sec.

The granular, mottled fracture noted in the two bars on the right of Fig. 7 is due to chilling or gassing by the basin core, rather than to differences in pouring rate in basins of three or six holes. The fractures indicate low melt quality, rather than the true, intermediate quality, of this melt. Examination of the tops of these blocks showed a flat surface and roughness characteristic of the A.F.S. 50 core sand used in the basins. The other four chilled blocks had convex top surfaces. It is probable that the change in fracture was due to concentration of gases at an intermediate point in the block, due to chilling from both faces

rather than to gases picked-up from the basin core, since previous data bars indicated no gassing due to core mold in contact with the molten brass.

It is also essential that a basin be used to control pouring rate. It is obvious from the above that the metal in the chilled block must not be in contact with the pouring basins. Therefore, provisions must be made for bleeding metal from the top of the block so that the basin core will not chill the top surface. This can be accomplished by notching the top of the chill block core to a depth of $\frac{1}{8}$ to $\frac{1}{4}$ in.

Quenching or Shake-Out Temperature

It is desirable that the time required for completion of the fracture examination be as short as possible, in order that the molten metal is not required to remain at its superheated temperature for an excessively long period of time. Therefore, the test block should be quenched, fractured, and examined in as short a time as possible. To investigate the influence of quenching temperature, six melts of varying quality were poured. A series of test bars were poured from each melt, and individual blocks were quenched in water after they had cooled to selected temperatures between 1800 F and 600 F. The temperature measurements were made in the blocks at a position 1 in. from the chill plates. Fractures obtained in blocks from melt No. 55 (intermediate quality-gas fired furnace) are shown in Fig. 8. Quenching temperatures and time required to reach these temperatures are shown in Fig. 8.

It will be noted that the blocks which were quenched from the highest temperatures exhibit a yellow gold, brittle type of fracture in the columnar zones. The most marked indication of this type of failure was in blocks three and four, which failed at positions about 1/4 in. from the notch used to control the location of the fracture. The uncontrollable fracture in the columnar zone apparently takes place through quench cracks developed in the blocks quenched in water from above 1500 F (temperature at a point 1 in. from the cast iron chiller).

However, the depth of blue-gray and appearance of the granular area in the slowly cooled end of the chill block were not influenced by the temperature from which the blocks were quenched. Therefore, for shop quality checks early shakeout and quenching can be used, but it must be realized that there is the possibility of cracks in the columnar zone.

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Influence of Prior Notching

One means of reducing the time required to complete the fracture test is to eliminate the necessity of cutting the chill block to provide a notch. This can be done by notching in the mold. Previous experience has indicated that the use of the sand notch is unsatisfactory, due to the danger of gas pickup from the sand. Therefore, it is necessary that the chilled blocks be notched with some inert material. This has been accomplished by the use of inserts of thin hacksaw blades, or 20 and 22 gage sheet steel. The metal notch has been kept to a minimum in thickness and depth to avoid a separate chilling effect from the steel. Chill blocks are notched to a depth of about 1/8 in. in a

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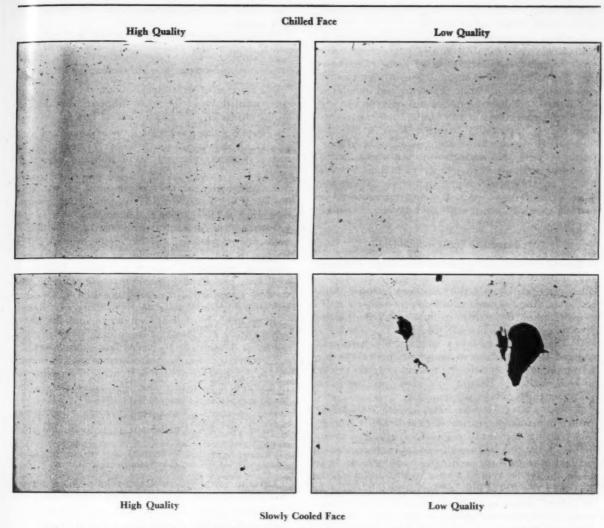


Fig. 11-Lead Distribution in Chilled Blocks of High and Low Melt Quality. Unetched-100 X.

direction perpendicular to the chill face. Photographs of typical fractures of notched and unnotched bars from gas furnace melt No. 55 which was of intermediate quality are shown in Fig. 9. It will be noted that the fracture appearance was not effected by notching with the thin steel inserts, and therefore, this procedure might be safely employed to reduce the time required for the fracture test.

Relations of Fractures to Macrostructures

Several of the chilled castings have been sectioned and ground for macroetching to secure a comparison of fracture and macro-etched sections. Melts varying in quality from very high to very low have been examined. A photograph showing five typical macro sections is shown in Fig. 10.

The macro sections show high columnarity originating from both the chilled face and from the mold surfaces in blocks from melts of high and intermediate quality (Specimens 4 and 5). The columnarity is

greater, the higher the quality. In bars from melts of lower quality the columnarity decreased; (Specimen 3) and very low quality metal showed practically no columnarity (Specimens 1 and 2), but had a rather fine granular appearance.

The macro appearances correlate well with fracture appearances and indicate clearly the dependability of the chill block for quality measurement. It is well known that best properties are secured in test bars having columnar fractures, and poor properties in bars having granular fractures.

Relations of Fractures to Microstructures

Several chilled blocks have been sectioned for microexamination to determine the relationships between fracture and microstructure. The examination was aimed principally at relating lead distribution to fracture patterns. Typical photomicrographs at 100X are shown in Fig. 11. These represent the structures of high and of low quality metal at two locations in chilled blocks; in the rapidly cooled lower section and in the slowly cooled upper section.

It will be noted that in the rapidly cooled section of both blocks the lead distribution was similar, with small, rounded, randomly dispersed particles. In castings this pattern would be indicative of high quality expressed in terms of tensile properties and soundness.

The lead distribution and general structures in the slowly cooled sections of the blocks varied markedly from that of the rapidly cooled sections in that the lead particles were coarser and tended to be aligned into a network. No microporosity was visible in the specimen from the high quality melt, but the low quality metal showed both micro and macro porosity. This structure represents the microstructure of a coarsely mottled fracture with gold facets in a reddish brown background.

Suggested Test Procedure

Based on experience gained to date in the investigation of chilled block fractures as indications of melt quality, a tentative procedure has been established for ted brass. A test is made as follows:

1. Superheat the melt to its final peak temperature,

according to the practice selected.

2. Tap 4 lb of metal into a No. 2 crucible previously charged with 2.0 grams of phosphor copper (15 per cent phosphorus) for deoxidation and 18 grams of zinc to compensate for melting loss (2 oz per 100 lb of

phosphor copper and 1 per cent zinc).

- 3. Pour a $\frac{3}{4}$ x 3 x 6-in. block against a blackened chiller immediately. In the interim between tapping and pouring, the metal temperature will drop about 100 F. A pouring basin with two, $\frac{1}{2}$ -in. holes should be used to control the pouring rate of the brass. The basin should be kept full to insure a rapid rate of pouring. The top of the core mold should be notched to "bleed" about $\frac{1}{8}$ to $\frac{1}{4}$ in. of metal from the top of the block, thus preventing cooling of the metal by the basin core.
- 4. The chill block is allowed to cool for 1½ min, which will bring its temperature to approximately 1500 F at a distance of 1 in. from the chilled face.
- 5. The block is then water quenched and fractured in a fixture of the type in Fig. 12.

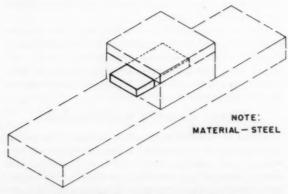


Fig. 12-Sketch of Fracturing Fixture

6. The fracture is examined and interpreted for melt quality.

The total elapsed time for fracturing and examination is about 3 min.

Interpretation of Fractures

The fracture of chill blocks obtained by the procedure detailed above should be inspected for the following characteristics, which indicate melt quality:

- 1. Depth of blue-gray color is the best index of melt quality. A depth of 2 in. or higher of fine structure with a definitely blue-gray color indicates high melt quality.
- 2. Columnarity is observed in most of the chill blocks. It has been observed that within the temperature range of 2200 to 2000 F, the columnar pattern will give a definite correlation with melt quality that is high columnarity denotes high melt quality. The fracture is rough and granular in metal melted in a reducing atmosphere, superheated above 2350 F, or held long periods at high temperature. Further, the fracture is mottled if the metal is gassed by late contact with a source of moisture or reducing gas. Brown spots of oxide result from air drawn into the upper surface of the chill block. Dross usually shows as green masses at the sides or air cooled face of the block.

Note: The absence of the columnar pattern may be due to the presence of high percentage of reducing gas in solution, or to inadequate deoxidation, or to pouring at temperatures below about 1975 F.

3. Appearance above the blue-gray may vary due to dross in the form of inclusions, or reducing gas other than moisture.

Conclusions

Certain tentative conclusions have been formed based on the data observed to date. These are:

- 1. Melt quality of 85-5-5-5 red brass can be correlated with fracture appearance of chilled blocks.
- 2. Melts of high quality have fractures showing at least 2 in. of fine blue-gray and a columnar structure of 3/4 in. from the chill face.
- 3. Intermediate melt quality is indicated by a chilled block fracture having somewhat lower bluegray and columnar depths with somewhat coarser fracture and a reddish, rather than deep blue, color.
- 4. Low melt quality chill blocks have little clear blue-gray and columnar patterns and tend to coarsen or roughen fractured surfaces. The fracture is often mottled due to high gas content.
- 5. Fracture characteristics are, within limits, independent of the pouring temperature in the range of 2200 to 2000 F, chill block dimensions, core mold composition, shake-out temperature, or notching of the chill block.
- Quality of the melt is independent of the furnace used, but is dependent on the melting practice.
- Appearance of macro sections of chilled blocks correlates well with fracture appearance, and substantiate reliability of the chilled block for quality measurement.

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Acknowledgment

The writers wish to express their appreciation to the American Foundrymen's Society for financial support in the research project from which data for this paper were obtained and to the Research Committee of the Brass and Bronze Division of A.F.S. for advice during the project.

The contributions of Y. Telang and W. Betts, students in the Department of Chemical and Metallurgical Engineering, W. B. Pierce, Instructor of Metal Processing, G. C. Evans and L. Ruffins of Metal Processing, in the performance of some of the research work are deeply appreciated.

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DISCUSSION

Chairman: G. J. BARKER, University of Wisconsin, Madison. Co-Chairman: H. J. ROAST, Consultant, Westmont, Que., Canada.

A. K. Higgins: We ran a limited number of fracture tests but unfortunately not enough to confirm results in this paper. We applied it to a number of 85-5-5-5 heats, all of them of high quality with one exception. We found that we did get the bluegray fracture extending up to 2½ in. and more when we had high test bar quality. Our lowest bar that came in that range had approximately 38 per cent elongation and tensile strength of 40,000 psi. We have unfortunately only one bar that fell below the 28 per cent elongation and in that case we used a backsaw blade for parting. We had such bad blows because of the hacksaw blade that we could not read the fracture.

We have also made a few tests on other alloys. The test seemed particularly well suited to the lead alloys where it gave good indications. Apparently it is possible to read the fractures on gunmetals. You do not get the blue-gray fracture but you do get a fracture that you can read. You get the difference between fine grain and coarse grain.

H. H. FAIRFIELD: ² Prof. Rote showed me this fracture test last September and we have been using it in production ever since, with satisfactory results. We pour the test specimen and as soon as it is hard, quench it in water. We have a pneumatic grinder with a flexible grinding wheel and we can notch and break the specimen before the ladle is poured. This gives us an idea of how much nitrogen to use.

It has worked out well for 85-5-5-5 and it is successful on high-copper alloys such as electrical conductivity copper and the 96 Cu-4 Zn alloy. We taper the specimen a little more than the original test. We believe this results in more directional solidification. The essential point is that we are able to break a test before the heat is poured and we know the metal will be right.

B. N. Ames: ^a What effect would lead have in quenching the specimen from elevated temperature on fracture characteristics?

MR. ROTE: We have introduced our fracture test procedure in foundries that have used it on high-lead content alloys and also on alloys with higher and lower copper contents. With the higher lead contents we observed a greater preponderance of quench cracks down the woody or columnar area. With higher copper alloys of low-lead contents there is a tendency towards disappearance of the quench cracks, characteristic of leaded alloys. Characteristic fractures appear not only in 85-5-5-5 but in the 88-8-4 and other alloys.

G. P. HALLIWELL. I wanted to hear more confirmation of this test from industry because I have a note of negation. I tried this test but attempted to cut corners so that the foundrymen could be helped. Perhaps I cut too many corners because I did not get all the characteristics in every case that the authors obtained in their paper.

In the first two heats, one of which was a very good heat and the other a very bad one, I got very fine differentiation and I thought the test satisfactory. In repeating the test we made the melting conditions even worse. So we made a perfectly good heat and then made a very poor heat and we could not tell the difference in the fractures.

I used a short cut by not using a pouring basin wherein you have a constant rate of pouring. Perhaps my foundrymen did not pour at the right rate so that the speed of chilling was not critical for the fracture test. Perhaps that is the error that I have made in using this test.

MR. ROTE: We encountered considerable difficulty at the beginning of our work with variation of pouring temperature and pouring rate. We finally agreed on the 2000 to 2200 F pouring temperature range with a fairly rapid pouring rate. A slow rate of pouring will give, even in low quality metal, a granular type of fracture, but it will be blue-gray, whereas if that same metal is poured at a more rapid rate and at a higher temperature, the fracture will go back to the reddish mottled type of fracture that is characteristic of low quality metal. It is necessary that you use a pouring basin, keep in full, and pour at a fairly rapid rate, otherwise the quality indication may be

It is important that the upper face is actually air-cooled and not chilled, as indicated in the illustration showing poor fractures from blocks made with three and six holes in the pouring base. We were confused for a while by that, until we found that the upper face did reproduce the finish characteristic of our A.F.S. 50 core sand and found we would have to bleed the chilled castings.

I think Mr. Fairfield's idea of tapering the block is a good one, because the taper provides a further differential in cooling rate that will be achieved with a straight side block.

Mr. HALLIWELL: Which way does the taper go?

Mr. Rote: The block is smaller at the chilled face and larger at the air-cooled end. Some of Mr. Fairfield's fractures, most in fact, were from good quality melts and they looked very good. What metal did you use?

Mr. FAIRFIELD: The specimens we sent you were 85-5-5-5. It might be well to point out that the chill block must be clean.

Mr. Rote: The chill block should be clean and perfectly dry. It cannot be too hot, of course. We dry our chiller, which is a machined cast iron plate, and blacken it with an oxy-acetylene torch. The purpose of blacking is twofold, to prevent sticking of the metals and to heat the plate sufficiently to drive off any moisture, which obviously would be bad from the standpoint of gassing on the chilled surface.

This entire test requires only 3 min for completion. It takes $11\frac{1}{2}$ or 2 sec to pour the casting, $11\frac{1}{4}$ to $13\frac{1}{4}$ min to complete solidification, at which time the block can be quenched. It must, however, be solid before quenching, otherwise there will be a false indication of quality. It may crack when quenched in water, but it will give, in the section above the quench cracked area the same indication of quality, regardless of quenching temperature. The casting can be quenched after about $11\frac{1}{2}$ min, and assuming a matter of $11\frac{1}{4}$ for breaking the block and a very few seconds for checking, you can do a complete test in 3 min.

MEMBER: Let us assume you have a fracture test indication of a bad quality metal. Does this fracture indicate just what is wrong with the melting practice? If you get a bad quality melt, what do you do with the metal that is left in the crucible?

MR. ROTE: This test will to a limited degree indicate the reason for the bad fracture. We have not completed the work on that part of the investigation as yet, but we have found characteristic fractures in the granular zone above the blue-gray area. We find that we get a distinct tan mottle in 85-5-5-5 that has been gassed by late contact with moisture, such as from wet clay on the surface. If we melt in a reducing atmosphere in a gas-fired furnace, the mottling occurs in the upper end of the bar but it has reddish brown facets rather than tan facets.

What to do with poor quality metal is probably what you do with it now; you put it in a pig mold, remelt it, and hope to get it back where you can make castings. Our test is designed to help you eliminate those heats which are going to make bad castings. You may lose your melting cost, but let us hope, as you use the test, you will eliminate the loss of castings which

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 Senior Metallurgist, New York Naval Shipyard, Brooklyn, N. Y.

Director of Research, H. Kramer & Co., Chicago.

may go through the foundry into the machine shop or to your

customer and come back as bad castings.

You can reclaim bad metal. We have never made high quality metal from low quality metal, but we have made intermediate quality metal, which in your plant will produce tensile strengths of 35,000 to 37,000 psi, ductilities of 25 to 30 per cent, and densities around 8.8 in the button end ASTM test bar. This is done by degassing with dry nitrogen, which seems to be an excellent procedure for bringing the metal back above specification. But it does not bring it the whole way to the optimum high quality. That is one thing you might do.

As Mr. Fairfield mentioned they use the degassing procedure and can determine what degree they should degas to get the metal up to what they consider the quality required for their

astings.

B. M. LORING: ⁶ This work is still in progress and we still have many factors to investigate other than those that we have at present covered. Although some of the reports may be very favorable at first, nevertheless, there are more factors to be investigated. For those who do want to use this test, I would suggest that you write to Prof. Rote before you do so and ascertain from him some further details. Be sure to report back to him directly some of the results of the first two or three attempts because you might make mistakes that you did not expect and obtain results which were far different than what you had hoped.

MR. ROTE: I am in agreement with that. In fact we solicit any results that you obtain. We are working on an A.F.S. sponsored research project and would like to cooperate with anyone who will use the test and will report back, if he so desires, so that we can get confirming or negative results from the tests. We are still working on it and have many things to iron out yet. We have given you some of the precautions that we have now. It has smoothed out and become reasonably successful but we are still working on it and will for several months yet, to standardize the test and get all the variables that we possibly can under

control.

G. M. Thrasher: 6 For many years I was associated with the valve industry making brass valves, etc. I am very much interested in this paper and the colored fractures presented. Your particular test is used more for making castings of large cross-section. But in making valves where there are a number of valves in each mold and with the body sections 1/8 in. thick or thinner, it is necessary to cast within a limited range of temperatures starting say around 2250 F. I found it necessary to check closely the final pouring temperature when pouring 22 to 25 molds. We found the foundry leakers are excessive in the final pouring, and in pouring from a steel ladle lined with a plastic mix, resulted in a temperature drop of 35 deg per minute. It took 5 or 6 min to pour the 200 lb of metal and the final 15 per cent were leakers due to cold shuts. As a final solution we adopted insulated pouring pots resulting in only 20 deg per minute temperature drop.

In the case of castings, such as gate valves, where the bonnet end is machined outside for decorative purposes and the internal thread results in removal of both surfaces, it is important to frequently break a casting or test piece in the foundry to be sure the composition is properly balanced for density and

pressure tightness.

As an example, if we take a piece of a runner ¾ in. square from a gate of castings, nick it with a hack saw, and break it, the fracture gives an indication if enough zinc has been added in melting and superheating or deficiency in the metal charged.

A sharp break with a uniform blue-gray fracture indicates good pressure metal running about 8.8 density whereas if you have a picture frame fracture of gray with brass or bronze color inside, the density may be as low as 8.6 in which case many valves will sweat at the bead under hydraulic pressure when assembled and tested.

Zinc has three important functions in these red and semi-red casting alloys and many foundrymen ignore all three.

The degassing effect is most pronounced if a piece of zinc is forced into the bottom of a ladle creating an evolution of heavy zinc vapor to drive out lighter gases.

Mr. Rote: We make adjustments in our melts for zinc loss. Those melts which are superheated to high temperatures receive

additions of 11/2 per cent zinc, and those at lower temperatures have a smaller addition of about 1 per cent zinc. We use copper-phosphorus deoxidation, and the investigation shows that the lar can be used to indicate the degree of deoxidation.

So far, we have concentrated on the presence of reducing gases as indicated by the fracture of the bar. It is possible, and quite probable, that we can also develop correlations of degree of

deoxidation and color and fracture appearance.

If you are pouring castings at higher temperatures than our limit of 2200 F, you can pour your chilled block casting at the higher temperature. You will get very similar fractues in the blue-gay area and similar colors; a blue color for high quality metal and a reddish color for the intermediate and low quality.

The difficulty that you will encounter at high pouring temperature is a high degree of columnarity. If you learn to associate columnarity with quality as correlated over the temperature range of 2200 to 2000 F and you suddenly go to a higher pouring temperature, you are going to find that, even in low quality metal, you will get considerable columnarity. That is the basic reason for the precaution that you pour at a temperature somewhere between 2200 and 2000 F. If you wish to continue with a pouring temperature for the chill block of say 2200 F, you might ladle out a small quantity of metal from the pot at high temperature, treat it with a small quantity of zinc and copperphosphorus and check it in the usual way.

Mr. Thrasher: There is too much dependence on phosphorcopper, I believe. In pouring light castings it is quite important to use insulated ladles unless you are pouring from very small

ladles and have a preheated crucible.

E. C. MANTLE: The BNFMRA we have been exercising a lot of persuasion to get British foundries to adopt various methods of degassing their melts and one of the chief difficulties has been that we have had no routine method of gaging the gas content of the metal. What we have had to do has been to assess the gas content by carrying out density determinations on test bars. The results of such determinations are only available long after the melt has been poured. This fracture test seems to enable a check on the gas content to be made while the metal is still in the furnace and so would be useful in determining the need for a degassing treatment as well tas assessing the effectiveness of the degassing treatment.

Frank Hudson: ⁵ Rapid routine tests, based on fracture characteristics, are new to us in Great Britain. We have been looking out for something such as this in order to get better control of the metal in the foundry and, personally, I am very interested in this fracture test that you are working on. It is something we do not know much about in Great Britain. I am leading a U. K. Brassfcundry Productivity Team in this country to study your methods. In this paper we found cut something which is well worth while including in our report and I think I can say that within a year you will get word from us letting you know how this method has turned out in a number of British brassfoundries. We would like to cooperate.

MR. ROTE (Authors' Closure): We wish to thank those who participated in the discussion of our paper, particularly Messrs. Fairfield, Halliwell and Higgins who reported results of tests of

the chilled block fracture bar which we propose.

We wish to repeat the notes of caution injected by Dr. Loring and Mr. Halliwell. This test is still in the development stages, with many variables still to be studied. Any foundry using the chilled blocks should follow a fixed production schedule to eliminate variables which might alter the fracture appearance. The casting should be poured at a rapid rate, and the metal should be at a controlled temperature, preferably between 2000 and 2200 F. All prssible sources of gas contamination, such as improperly dried chiller plates, wet ladles or crucibles, or wet additives, should be avoided.

Any improper practice employed in the production of the chilled block will provide an indication of poorer, rather than higher, melt quality. For that reason, the test can be considered as a conservative one; any metal which shows a fracture characteristic of good quality can be poured with assurance. On the other hand, good quality metal will often have a fracture indicative of poor quality, if the production of the chilled block is improper.

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TENTH ANNUAL REPORT ON THE INVESTIGATION OF STEEL SANDS AT ELEVATED TEMPERATURES

By

R. G. Thorpe,* Peter E. Kyle,** John P. Fraser***

Preface by Chairman C. Jenni

THE TENTH ANNUAL REPORT on the investigation of properties of steel foundry sands at elevated temperatures covers the work done at Cornell University during the year 1949-50. The work was performed by R. G. Thorpe and John P. Fraser, A.F.S. Investigators, under the direction of Prof. Peter E. Kyle. The basic problems and program were outlined by the Physical Properties of Steel Foundry Sands at Elevated Temperatures Committee.

The information given in Part I of the report is illustrative of the continuing work dealing with the fundamental studies on the behavior of sands on heating and cooling and the stress-strain characteristics. The work reported upon in Part II is not only of extreme interest but offers great possibility that test data obtained by elevated temperature testing may be applicable to the elimination of certain casting defects which may be attributable to the sand mixture.

The Committee is pleased with the progress being made on the project and anticipates an ever increasing yield of fundamental information which may be translated into improved foundry operations. The Committee wishes to thank the investigators for their interest and the work they have done; likewise, the continued interest and guidance of Dr. H. Ries and Prof. Peter E. Kyle, Cornell University are deeply appreciated.

Members of the sponsoring Committee 8-L are:

| Clyde B. Jenni, Ch | airman |
|----------------------------|------------------|
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| C. W. Briggs | R. E. Morey |
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| H. N. Kraner | D. C. Williams |

Introduction

This report covers the work on the Sand Research Project sponsored by the Sand Division of the American Foundrymen's Society which has been completed during the year 1949 at Cornell University. Part I summarizes the load-deformation testing and stress-strain characteristics of two synthetic sand mixtures at elevated temperatures and Part II discusses the pre-liminary test work completed on the scab-forming tendencies of the two sand mixtures.

Those associated with the project at Cornell wish to acknowledge the many helpful suggestions and guidance given by the Sand Division Committee on Physical Properties of Steel Foundry Sands at Elevated Temperatures, under the chairmanship of C. B. Jenni,† and particularly the advice given by Dr. H. Ries, Chairman of the Sand Division. The Sand Research Project is highly indebted to the New Jersey Silica Company, to Eastern Clay Products, Inc., and to the American Colloid Company, who donated all of the raw materials used in the investigations, and to the Harry W. Dietert Company for special items of equipment.

PART I

Load-Deformation Testing of the 10% Fire Clay (No. 7) and 4% Western Bentonite (No. 5) Mixtures

The routine procedure used for load-deformation testing is similar to that used in the hot compressive strength test1,2 except that a dial gage is used to indicate the dilatometer table travel (Fig. 1). Simultaneous readings are taken by two observers of load and table travel beginning at the instant of specimen contact with the upper loading post up to the maximum load at 50-psi increments of pressure as indicated by the pressure gage. The observed table travel is corrected to give the actual deformation of the specimen by subtracting the deformation of the loading system, as found by making a series of tests at various temperatures without having a specimen in place between the two refractory disks. The tare curves are given in Fig. 2 and 3. From the data, the stress-strain diagram is easily plotted. At least five specimens are tested and the average of the curves used as characteristic of the sand.

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^{***} Formerly A.F.S. Investigator, Cornell University; now Corrosion Engineer, Shell Development Co., Emeryville, Calif.

[†] General Steel Castings Corporation, Eddystone, Pa.

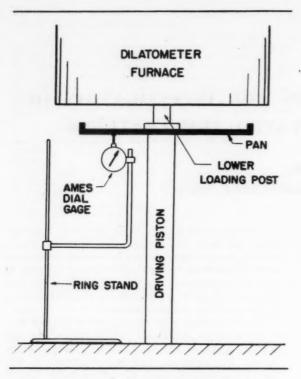


Fig. 1-Sketch of apparatus for load-deformation testing.

Typical stress-strain diagrams are shown in Fig. 4 for the 4 per cent Western bentonite (No. 5) mixture. The average curve is given in Fig. 5 for the 10 per cent fire clay (No. 7) mixture. In each case, the stress-strain relationship is shown for a temperature below and above the peak strength temperature of the mixture.

In general, it would be expected that the stressstrain curve for a sand specimen would have three distinct portions, corresponding to (1) closing up of any cracks which have developed on shock heating, (2) a nearly-linear increase of strain with stress, which is believed to result from closing up of the voids in the specimen plus some plastic deformation of the bond, and (3) more rapid deformation just prior to failure, caused by plastic deformation and actual failure of the clay bonds. It should be noted that the clay bonds, and not the sand grains themselves, fail. This would be expected since silica shows higher strengths at all temperatures than any of the sand mixtures tested,3 and is borne out by microscopical examination after test. Above the peak-strength temperature, step (3) is the result of plastic flow as well as failure of some of the clay bonds, and considerable strain occurs in the specimen with decreasing stress, after first reaching a maximum. The amount of plastic deformation increases as the temperature increases.

The three portions of each curve, referred to above, are quite distinct in Fig. 4 and 5. The increase in magnitude of portion (1), the closing up of the cracks produced on shock heating, is believed to be directly

related to the free-expansion characteristics of the sand.

The variation of maximum strain and hot compressive strength with furnace temperature is shown in Fig. 6 and 7 for the two mixtures, both tested at 20-min exposure time. The actual data spread at the upper portion of the strain curve is excessive, and is being investigated further. Figure 7 is the average curve for the fire clay mixture. In general, it is seen that the maximum strain increases almost linearly with hot compressive strength up to the peak strength temperature, after which point maximum strain continues to increase due to increased plastic flow of the clay bond phase, although the strength decreases sharply.

Effect of Loading Rate on Stress-Strain Diagram

Since other investigators^{5,6} have reported that hot compressive strength generally increases with increased loading rates, it was felt that the actual shape of the stress-strain curve might be a function of the rate of loading.

The experimental procedure used is similar to that described earlier for routine load-deformation studies. Since the dial gage and pressure gage readings could not be read accurately at the higher loading rates by two observers, motion pictures were taken of the necessary gages throughout the course of each test. A timer was mounted on the front of the dilatometer near the pressure gage, and the camera positioned so that the deflection dial, timer, and pressure gage could be photographed. The camera was operated with a shutter speed of 1/40 sec, at 24 frames per sec. The camera was started when the dilatometer pump

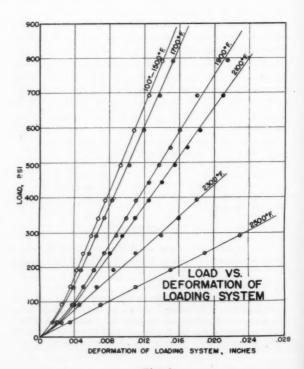


Fig. 2

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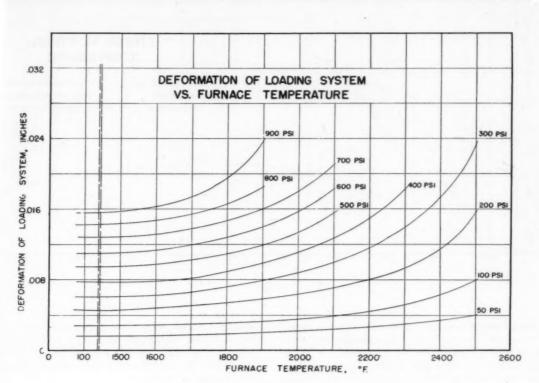


Fig. 3

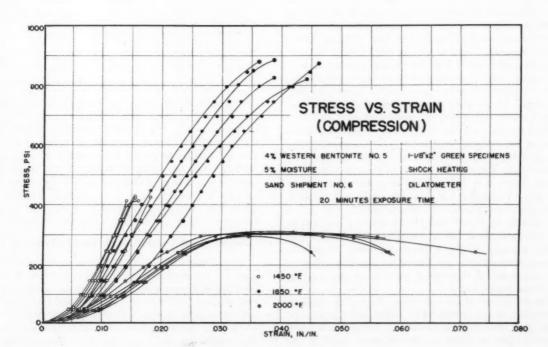


Fig. 4

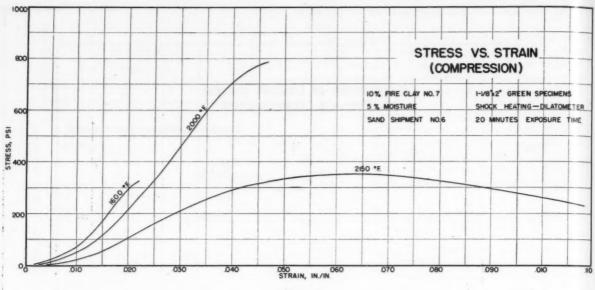


Fig. 5

was turned on and stopped after failure of the specimen occurred. The necessary dial readings were taken from the film, a simultaneous plot made of the deflection dial gage reading and pressure gage readings against timer readings and ultimately, the stress-strain diagram plotted.

The sand mixture used for the test was the 4 per cent Western bentonite and 5 per cent moisture mix using the New Jersey No. 60 base sand. The tests were conducted at 1500 F, seven specimens broken at the lower no-load limit of dilatometer table rise (0.919 in./min) and seven at the highest rate (1.84 in./min). Figure 8 shows a typical plot of the original data at the lowest rate, while Fig. 9 gives comparable data at the highest rate. The measurement lag in the dilatometer hydraulic system amounts to about 0.5 sec, hence the deformation curve is shifted slightly to the right from its true position on the data plot.

Since the loading rate (expressed as psi/sec) is the slope of the loading curve at any point, and similarly, the straining rate (expressed as in./in. sec) proportional to the slope of the corrected deformation curve at any point, it is immediately apparent that neither the rate of loading nor rate of strain are constant throughout the course of any one test.

It should be noted that added validity is given to the statement made earlier regarding the shape of the initial portion of the stress-strain curve as due to closure of the cracks developed on shock heating by the curvature of the initial portion of the loading curves in Fig. 8 and 9.

Figures 10 and 11 are composite plots of the loading curves and strain curves. The strain curve is derived from the corrected deformation curve mentioned above simply by dividing the ordinate scale by the specimen length. It should be noted that the upper portions of the loading curve are linear and parallel, indicating that final average loading curve may be held constant from one determination to the next. An

average overall strain rate may be derived from a series of tests by drawing an average line through the corrected deformation-time curves, but it should be noted that the strain rate is not constant throughout any portion of the test.

The following data were obtained from Fig. 10 and 11:

| No-load rate of table travel (in./min) | Average Final Loading Rate (psi/sec) | Average Overall Strain Rate (in./in./sec.) | Average Maximum Stress (psi) | Average Maximum Strain (in./in.) |
|---|---|---|---------------------------------------|---|
| 0.919 | 55.2 | 0.00144 | 536 | 0.0208 |
| 1.84 | 110.5 | 0.00282 | 450 | 0.0188 |

The stress-strain curves for the specimens tested are shown in Fig. 12 and 13, while the average curves for each are given in Fig. 14. The curves show that while the average maximum stress in compression and the average maximum strain is decreased at the higher loading rate, the general shape of the stress-strain curve is uneffected. Further tests will be required to indicate the significance of the change in maximum stress and maximum strain with change of loading rate in the light of variation between specimens.

Discussion and Conclusions on Load-Deformation Test

The difficulties which have been encountered in the past by foundrymen attempting to obtain a broad correlation between sand performance in the foundry and the accepted laboratory room and high temperature sand tests clearly indicates that we must extend our knowledge of the fundamental mechanical properties of molding sands, particularly at elevated temperatures. The load-deformation test is rapidly approaching a final stage of development, and will be offered to the foundryman as a tool by which he may more clearly evaluate the performance of a sand under load

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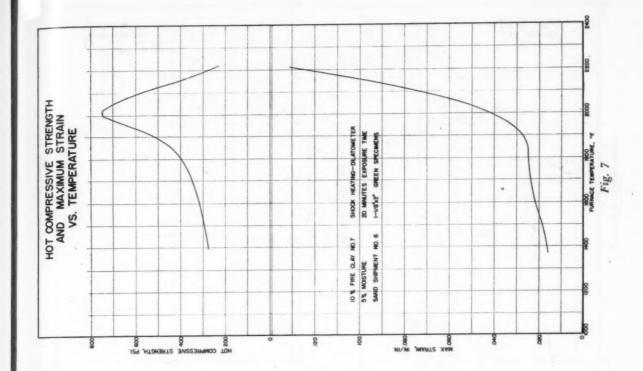
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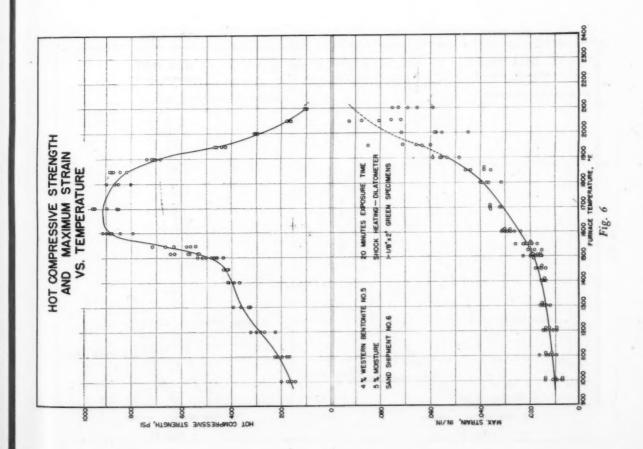
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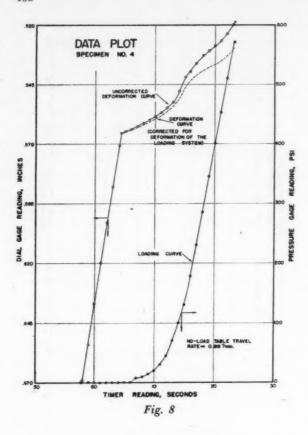
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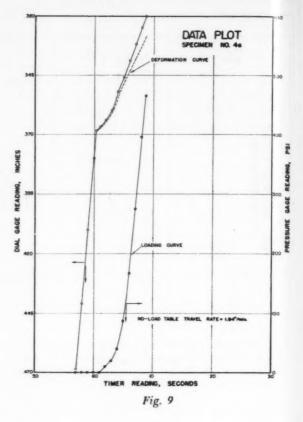
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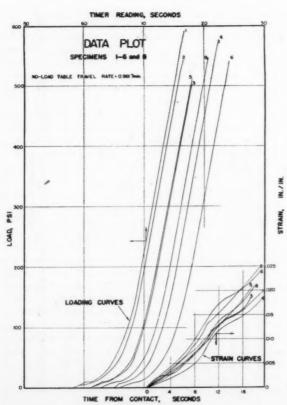
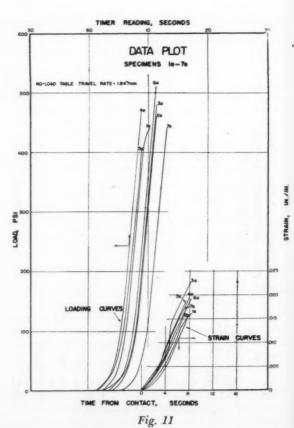


Fig. 10



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GAGE READING,

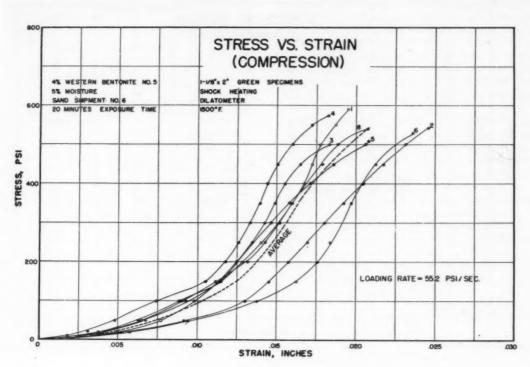


Fig. 12

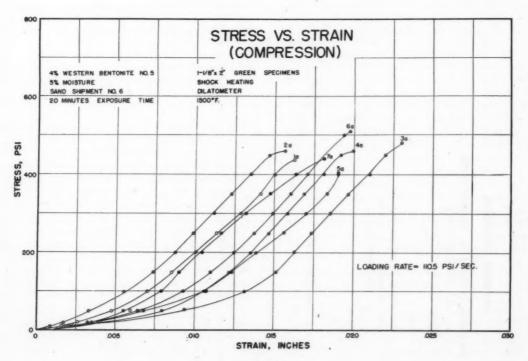


Fig. 13

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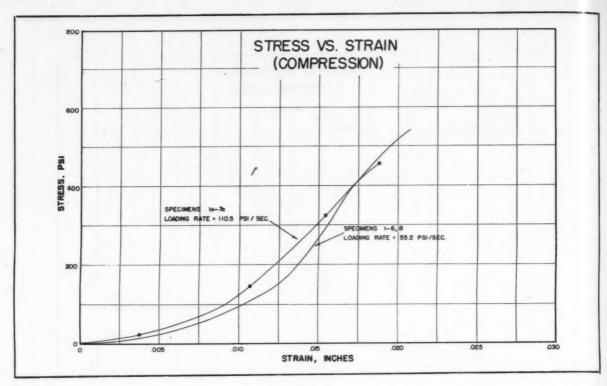
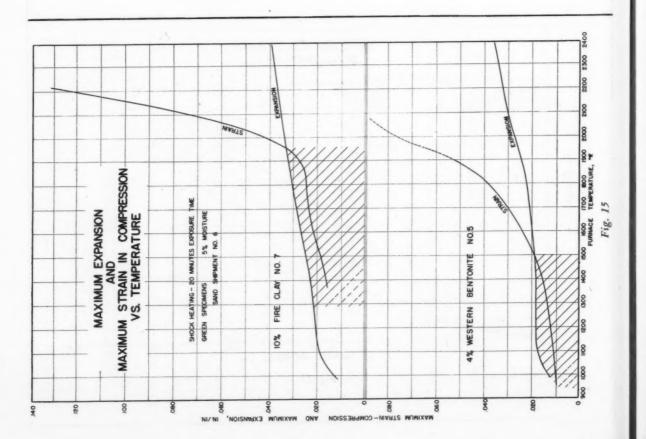


Fig. 14



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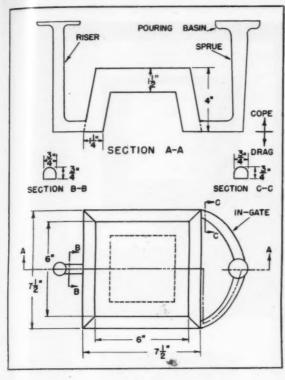


Fig. 16

in a mold. The preliminary work reported here seems to indicate that differences in stress-strain characteristics may offer explanations of variations in sand performance in the foundry. The maximum strain obtained in this test when correlated with expansion characteristics in Part II of this report, appears to offer an explanation of the scabbing defect.

PART II

Scab-Forming Tendencies of the 10 Per Cent Fire Clay No. 7 and 5 Per Cent Western Bentonite No. 5 Mixtures

The practical aim of all foundry control testing and ultimately, of basic research on foundry sands, is in the prediction and elimination of those casting defects which can be traced to the sand mixture itself. It is felt that the occurrence of rat-tails, seams, buckles, scabs and drops are the result of high expansion of the sand on shock heating, coupled with a small ability to deform under load. That is, if the mold-metal interfacial conditions are such that the expansion of the sand exceeds the maximum strain, part of the sand will tend to break away from the body of the mold in order to relieve the stress and a casting defect will result. A comparison of maximum strain with expansion on shock heating* is shown for the 4 per cent Western bentonite (No. 5) mixture and for the 10

per cent fire clay (No. 7) mixture in Fig. 15. A potential defect producing zone (cross hatched) exists for both of the sand mixtures used in this investigation at the test conditions employed, but it is apparent that the fire clay (No. 7) mixture would be more likely to produce defects than the Western bentonite (No. 5) mixture, since the cross-hatched zone extends to higher temperatures, i.e., to 1950 F for the fire clay (No. 7) mixture as opposed to 1500 F for the Western bentonite (No. 5) bonded sand.

It is felt that although the relation between expansion and strain is significant, other properties of the sand are also important. The heat transfer characteristics of the sand will, for example, determine the rate at which the temperature of the interfacial sand will

pass through the defect producing zone.

It is realized that although a tremendous amount of laboratory data may be accumulated over a long period via a research program of this type, the overall worth of the data will eventually be fixed when metal is poured in a mold. In order to obtain a preliminary practical confirmation of the expansion and stress-strain testing previously described in this and earlier reports, 1,2 and further, to acquire some essential information prior to the start of a large scale casting program, it was decided to check the two synthetic sand mixtures in the foundry.

The Fairfield scabbing pattern was used (Fig. 16) and because of the qualitative nature of the data desired, the molds were hand rammed in 16-in. x 16-in. flasks using a 6-in. drag and 6-in. cope. The parting line was dropped down 1 in. in the drag so that the mold cavity was formed entirely in the cope with 3 in. of sand over the top of the mold. An attempt was made to ram each mold to a uniform mold hardness.

The sand mixture under test was used as facing sand to a rammed depth of 1 in. over the top and on the sides of the pattern. The cavity under the pattern was filled completely with facing sand. The foundry heap sand (107 Medium New Jersey Molding Sand) was used as backing, and all molds were gated as shown in Fig. 16. Six molds were poured; three in the 10 per cent fire clay (No. 7) mixture and three in the 4 per cent Western bentonite (No. 5) mixture.

Pending the completion of the installation of steel melting facilities, these molds were poured in gray iron during a scheduled class cupola heat. It is realized that any trends evident in this experiment will be aggravated at steel pouring temperatures. Following pouring and shakeout (1½ hr after pouring), the castings were shotpeened lightly to remove adhering sand, inspected, and photographed. See Table 1.

TABLE 1

| Mold N | | cing | Cope | Drag | Defect |
|--------|---------|-----------|-------|-------|---|
| | W11 | Fire Clay | | | Detect |
| 1 | Fire | Clay | 70-75 | 65-75 | Small scab on side. |
| 3 | | 29 | 65-75 | 60-65 | Badly scabbed on top surface and bottom. |
| 5 | 99 | 99 | 70–80 | 70-80 | Badly scabbed on top surface. |
| 0 | Western | Bentonite | 75-85 | 75-85 | Small scab on bottom. |
| 6 | 9.9 | | 70-85 | 65-70 | Good casting. |
| 8 | 99 | 9.9 | 65-70 | 60-75 | Good casting. |

^{*} The apparatus and procedure used for the determination of free expansion on the 1-in. x 1-in. x 8-in. specimen has previously been described.*

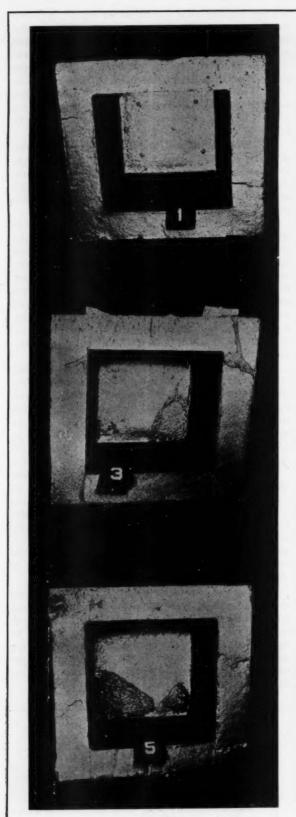


Fig. 17

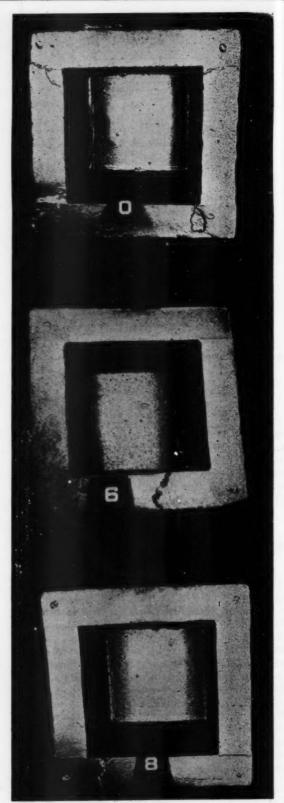


Fig. 18

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The green sand properties were determined as shown in Table 2.

TABLE 2

| I ABLE 2 | | | |
|---|-------|---|--|
| | | 4% Western Bentonite (No. 5) Bonded Facing | |
| Moisture, per cent Green Compressive | 4.6 | 4.7 | |
| Strength, psi | 3.8 | 3.5 | |
| Permeability | . 127 | 160 | |

Castings 1, 3 and 5 are shown in Fig. 17; 0, 6 and 8 in Fig. 18. An inspection of the two figures will show that under the conditions used for the experiment, with the particular gating system used, the 10 per cent fire clay mixture appears to produce a scab defect more readily than does the 4 per cent Western bentonite mixture.

It is emphasized that no attempt has been made to eliminate the scab defect as described above by means of changes in sand mixture composition or alternate gating systems. This preliminary experiment was designed to compare the two sand mixtures under the same molding and pouring conditions.

Conclusion

From these preliminary tests, it appears that the scab forming tendencies of molding sands may be predicted on the basis of the relationship between the fundamental properties of expansion and hot deformation. Further work may show a similar correlation between these properties and other defects

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DISCUSSION

Chairman: C. B. Jenni, General Steel Castings Corp., Eddystone. Pa.

Co-Chairman: J. H. Lowe, Wehr Steel Co., Milwaukee.

BRUCE BOECKER: The authors are to be congratulated for presenting such an excellent paper. In the future it may be necessary to tie in the chemistry with the physical properties of your sand. If you study the relation of the chemistry of the sand to the physical properties you may determine how to adjust its physical properties so that the sand-metal interface will be stable rather than unstable.

CHAIRMAN JENNI: That is perfectly true. We know that binders such as bentonite or clay have a considerable variation in properties and now you are introducing another variable, the matter of the treatment of these materials to produce variable properties. The point is that in this investigation there are many things that we can do to raise the one curve or to lower the other. That is the type of study we will go into next.

¹ Sand Technician, The Minster Machine Co., Minster, Ohio.

REPORT ON A.F.S. STEEL DIVISION RESEARCH PROJECT

By Clyde Wyman*

A FUNDAMENTAL STUDY of the influence of mold and core sand properties on development of hot tears in steel castings is the current research project of the Steel Division. Effect of these properties has been reported in a general way through observations of castings in the shop. However, no direct evidence has been presented as to which of the many sand properties measured can be used as the criterion to predict formation of hot tears. It was, therefore, the desire of the A.F.S. Steel Division to establish such a criterion, if possible, by this investigation. This research project has been carried on at Armour Research Foundation of Illinois Institute of Technology under the direction of Charles Locke, Supervisor of Foundry Process Research.

The first report, submitted by Armour Research Foundation to the A.F.S. Steel Division Research Committee, covered the period from the start of the project to the middle of July, 1948. In their report, the variables that had to be controlled were noted. Metal of uniform analysis and pouring temperatures must be produced by a standard melting technique. The design of the particular test piece is also most important. Some shapes are such that their inherent cooling stresses, coupled with hot spots, would cause them to crack, even if cast in an imaginary medium of perfect collapsibility. The test casting, then, must be of such shape and size that its contraction will be resisted by sand whose properties can be varied and controlled at will. The design must also permit measurements of sand resistance through dimensional changes of the casting and ultimately through the appearance of hot tears. An orientation program was, therefore, determined upon to decide on the fitness of test castings and procedure as suggested.

Initial Test Castings

The initial test castings were to consist of cylinders of ½-in. wall thickness and 4 in. in height. For a given series, they were to vary on their inside diameter from 1 in. to 6 in. in 1-in. increments. A variation in diam-

eter was chosen in order to have the sand of the core subjected to an increasing amount of total contraction. However, only the cylinders of 1-in. and 6-in. ID were used in order to check the extreme conditions. In addition, a very weak sand and a very strong sand were used for each pair to again note the response of the test castings to extreme properties in the sand. Furthermore, it was decided to minimize hot spots and shrinkage. For this reason, a full ring riser, through which the castings were top poured, was used.

The 1-in. ID cylinders showed no hot tears whatsoever, and the 6-in. ID cylinders produced too small an amount, even with the strong core, to be considered successful as a test piece. This caused the Research Committee to change the cylinder dimensions so that a ½-in. thick wall for the 6-in. ID cylinder and a ½-in. thick wall for a 12-in. ID cylinder were chosen. These castings were also poured with a full ring riser and both a green and supposedly strong core were used.

The results of tests made with these cylinders show that very small hot tears resulted with the dry sand core and that no tears were caused by the green sand core. The following tabulation is included to illustrate that the green sand core permitted the 12-in. ID cylinder to shrink at the standard rate of 1/4 in. per ft.

| Core Mix | Core Size, in. | Castings ID, in. | Remarks |
|--------------|----------------|------------------|--------------|
| dry sand | | | |
| 30% Si flour | 12 | 1129/32 | Slight tears |
| 3% Dextrine | | , | |
| green sand | 12 | 1124/32 | No tears |
| dry sand | | | |
| 10% Si flour | 12 | 1128/32 | Slight tears |
| 1% Core oil | 12 | 1128/32 | No tears |

However, work with the 6-in. and 12-in. ID cylinders showed that both were unsatisfactory as a test piece. The ½-in. thick cylinder was too prone to hot tears even with weak green sand cores, and the opposite occurred with the ½-in. thick 12-in. ID cylinders. It was, therefore, recommended that the cylinder test design be abandoned as a means of evaluating mold materials for hot tear producing properties and that

Metallurgist, Burnside Steel Foundry Co., Chicago and Chairman of the A.F.S. Steel Division Research Committee.

efforts be directed towards development of another

In the fall of 1948, the ½-in. thick, 6-in. ID test pattern was sent to the Burnside Steel Foundry, Chicago, in order to carry on an investigation in a production foundry for comparison with the work done at the Armour Research Foundation.

Core Mixtures Used

There were eleven different core mixtures used in this investigation at Burnside Steel Foundry. They consisted of green sand and dry sand of various oilsand mixtures made out of a dry portage crude sand with an average A.F.S. fineness of 60. The inches of tear, in the resulting castings, ranged from 0 to 52. There was a definite correlation in measuring the inside diameter of the casting as to the degree of tear. From this series, in using the various core mixtures, the higher oil-silica flour combination contents showed the highest degree of tear ranging proportionately down to the medium and lower oil-silica flour combinations in practically all cases.

A Steering Committee meeting was held on January 5, 1949, to review the past work and to outline the foundry program of the project. The work planned was to have equipment constructed for cylinders of 3 and 6-in. ID with wall thicknesses of 1/4, 1/2 and 3/4 in. These cylinders were cast in one mold with and without risers for each particular sand mixture used. A dry sand runner system, to permit the gates to be disconnected from the casting as soon as the mold cavities were filled, was employed. The runner core also operated so that all six castings could be filled at the same time thus minimizing the temperature differential between them.

Three core mixtures were chosen by the Research Committee for the initial test with the new cylinder sizes. These mixtures contain 0, 15, and 30 per cent silica flour with a sufficient quantity of cereal to make them handle without breaking in the green state. In all cases, there was no indication of tearing of the casting from visual observation but the amount of cutting or washing had occurred where the metal impinged against the core.

Twelve of these cylinder test castings were etched in a boiling solution of 1:1 hydrochloric acid which showed a development from 0 to 5 degrees of tear.

Based upon the results of the work done with the multiple cavity mold, it was quite evident that some of the tests performed had sand mixtures which did not behave as expected. Where hot tears should have occurred, none were found and vice versa. This condition indicated a lack of sufficient knowledge of the behavior of our foundry sands. The need for a test that will predict formation of hot tears due to sands resistance was even more apparent.

Pouring Range

All of the castings in the last series were poured within a temperature range of 2930 F and 2950 F. Temperature readings were taken with an immersion type thermocouple used in the ladle just prior to pouring.

The conclusions which can be drawn from the first year's work on this project are:

1. The design of casting used to date are inadequate as test pieces for the purpose intended.

2. The high hot strength mold materials do not consistently cause certain hot tears in steel castings of the design tested.

The work of the past year has indicated further problems for study in connection with the cause of hot tears in steel castings. From the inconsistent previous results, it is quite obvious that other factors in conjunction with the mold materials will have to be studied in order to obtain the information desired. The hope that these factors could be held constant was not realized.

Reproducibility of Tests

In order to determine reproducibility of the tests, a cooperative program was set up between Armour Research Foundation and the Burnside Steel Foundry. A detailed outline of procedure was set up by the Foundation, a copy of which was sent to Burnside Steel for their use in carrying out the tests to be made by them. For the first series of test castings, three modifications of the prescribed outline were made. The first change was that the sand mixture chosen by the Foundation would not be used by Burnside Steel and the second was that each agency would make and bake 24 cores instead of exchanging batches of sand. By so making the cores, the variables of shop methods of ramming and making were minimized and baked cores were exchanged. A third deviation was that Burnside Steel did not use the special ramming device that was built at the Foundation for making their cores. The chemical analyses were essentially the same for both with sulphur content of 0.036 per cent at the Foundation and 0.039 per cent at Burnside Steel Foundry. Aluminum additions in the amount of 21/2 lb per ton were used at both plants. A study of the results obtained by Burnside Steel and the Foundation show the following conditions to exist.

Existing Conditions

- 1. In the Burnside Steel test, using their own cores, the degree of tear ranged from 0 to 9½ in.
- 2. In the Burnside Steel tests, using the Foundation cores, a degree of tear ranged from 1 to $7\frac{1}{2}$ in.
- 3. In the Foundation test, using their own cores, a degree of tear ranged from 31/2 to 9 in.
- 4. In the Foundation tests, using only four Burnside cores, a degree of tear ranged from 7/8 to 35/8 in.
- 5. In all of the Foundation castings, the hot tears were located directly below the ingates. These were unavoidable hot spots due to the method of pouring even though no ingates were attached to the casting or riser.
- 6. A general observation is that the inside diameter increases with the greater degree of hot tear.
- 7. The type of hot tear that occurred in the Foundation test castings varied from short and shallow to long and deep. In many castings, the tears were completely through the wall section and the full length of the castings.

8. These castings exhibited a great deal of inconsistency with respect to the degree of hot tear produced.

In addition to the cooperative test, made by Burnside Steel and the Foundation, Burnside also made a number of other tests. Many castings were poured in which information was gathered concerning the core density, pouring temperatures, dimensional changes, and woodflour additions. A summary of these results shows trend, and in most cases, the trends are quite widespread. Some of the trends indicate that as the inside diameter of a resultant casting increases, so does the degree of hot tear. Another illustration is that as the core density for a given sand mix is increased, the degree of tear also becomes greater. A trend towards reducing hot tears with reduced pouring temperatures was also indicated.

Core Density and Hot Tears

Core density and hot tears were studied by the use of blown and hand rammed cores and by the use of varying amounts of woodflour to the core mixture. The woodflour affected the collapsibility of the core at high pouring temperatures and influenced the reduction of hot tears.

One series of tests was also made by Burnside Steel Foundry using green sand cores. The results were that no hot tears were found in any of the castings.

In order to evaluate the data obtained, some sort of consistency must be obtained. It is the aim of this project to determine which binders and in what quantities they should be used to overcome hot tear defects. As stated many times in the past, a satisfactory test piece must be available to make the required evaluations. From the data presented in this report, it is quite apparent that such a test piece has not been developed. Being able to indicate trends is of some value in a general way. However, for the purpose of this research project, it is necessary to have a test that will produce results which will fall within a consistently narrow range. If this can be accomplished, the evaluation of binders and quantities of binders and the relation to hot tear formation can be undertaken with a reasonable degree of accuracy.

Recent work has dealt with a test piece designed at

the Research Laboratory of the American Steel Foundries for hot tear studies. Its use was prompted by the desire for greater reproducibility and had been afforded by previous test pieces. The American Steel Foundries design still made use of a cylinder as the means of causing sand resistance to oppose casting contraction; however, a hot spot was incorporated along the length of the cylinder in order to concentrate the tears in one location. The cylinder is 4 in. long with a 10-in. ID and a ½-in. thick wall. An additional ½-in. by 2 in. of metal formed the hot spot. A full ring riser and top gate are used.

Gating arrangements are such that four castings can be poured simultaneously. In addition, a wall 7½ in. by 7½ in. by 2 in. is located below the sprue to equalize the pouring rate and metal temperature between four test castings.

The conditions under which this test design can give reproducible results have proven to be controllable. An extensive investigation of sand properties with emphasis on compressive strength from room to elevated temperatures is now under way.

At the last Research Committee meeting, the following members were present. C. Wyman, Chairman; R. H. Frank, G. W. Johnson, G. A. Lillieqvist, C. H. Lorig, S. C. Massari; members absent were H. A. Young and C. W. Briggs. It was tentatively planned that a test program is to be prepared by the Armour Research Foundation and an individual letter sent to top management of a number of foundries requesting them to cooperate by conducting tests in accordance with this outline. It is hoped through such a procedure that sufficient test data will be obtained so that an analysis may be made for evaluating the hot tear test casting. It is also hoped to establish the number of such castings which must be made in order to obtain consistent and reliable results.

Committee Personnel

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Present members of the Steel Division Research Committee are as follows:

| Clyde Wyman, Chairman | R. E. Kerr |
|----------------------------|-------------------|
| R. H. Frank, Vice-Chairman | G. A. Lillieqvist |
| C. W. Briggs | C. H. Lorig |
| G. W. Johnson | H. A. Young |

INFLUENCE OF DRY SAND CONDUCTIVITY ON RATE OF FREEZING OF STEEL SLABS

By V. Paschkis*

STUDIES carried out by the Heat and Mass Flow Analyzer Laboratory at Columbia University for the Heat Transfer Committee have considered so far^{1,2,8,4} variations in shape and size of the casting and different thermal properties of the casting, steel or otherwise.

The properties of the sand mold however have always been held at the same values which originally had been selected from handbooks. These values yielded on the analyzer solidification times which closely check with times obtained in bleeding tests.

In the meantime the A.F.S. Heat Transfer Committee had a number of sands tested by the Battelle Memorial Institute.⁵ The Institute has determined the conductivity for various kinds of dry sand as function of temperature.

Object of Study

The object of the present investigation was to find out how the time of solidification changes if sands with different properties are used as mold material. The Heat and Mass Flow Analyzer Laboratory being an analog computer, carries out by its experiments calculations which cannot be made by known mathematical techniques. But yet the experiments are essentially computations and these computations can be made with any desired values for the variables, e.g. for the conductivity, specific heat etc. If, in the text reference is made to "casting in a sand mold," to "temperatures" etc., it should be understood, that it would be more accurate to say, "the calculating circuit represented casting in a sand mold," "voltages representing temperatures," etc.

The present investigation was carried out for a steel slab, the steel having the same thermal properties as previously used in the analyzer experiments.

The sands which were used in the present investigations have been described by C. F. Lucks and coworkers.⁵ The investigation covers the three sands designated by Lucks as: 20-30; 50-70 and No. 7.

In all investigations carried out on the analyzer to

date, the sand properties were considered to be independent of temperature. The charts by Eucks and coauthors show that at least for some sands the conductivity varies quite appreciably with temperature. Now, the temperature of the sand near the mold-casting interface is much higher than at a greater distance. Hence the conductivity of the sand is not the same at different distances from the interface. Nevertheless, for the sake of comparison in three of the four experiments the conductivity was assumed to be independent of temperature. In one experiment a higher sand conductivity was introduced in the computing circuit next to the interface than further away.

Description of Casting and Sand

The casting which was investigated was a slab of steel, 4 in. thick and with so great an area that end effects could be neglected; the solidification was purely unidirectional, perpendicular to the surface of the casting.

The casting was made in a sand mold; either side of the casting was covered by a layer of sand 5 in. thick.

The steel properties were as follows:

| Conductivity, | | | Btu/ft hr. F |
|----------------------|----------|--------|-----------------|
| Conductivity, | solid | | Btu/ft hr, F |
| Specific heat, | liquid . | 0.200 | Btu/lb, F |
| ** | solid | 0.165 | Btu/lb, F |
| Density | liquid | | lb/cu ft |
| " | solid | | lb/cu ft |
| Heat of Fusion | | | Btu/lb |
| Solidification range | | | -2600 F |
| Pouring temperature | | 2845 | - |
| The sand was assume | ed to ha | ve a s | pecific heat of |

TABLE 1-LIST OF CONDUCTIVITIES AND DENSITIES OF SAND

| Exp. No. | Designation* | Thermal Conductivity Btu/ft hr, F | Density lb/cu ft |
|-------------|----------------------|---|---------------------|
| 1 | 20-30 | 0.400 | 108 |
| 2 | 50-70 | 0.275 | 102 |
| 3 | No. 7 | 0.208 | 91 |
| 4 | 20-30 | Variable | 108 |
| a Des | signation as per Luc | rks and co-authors | |

^{*} Technical Director, Heat and Mass Flow Analyzer Laboratory, Columbia University, New York City.

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0.28 Btu/lb, F. Table 1 contains the densities and conductivities for the sands used in the experiments. For Experiments 1 to 3 the conductivity was selected for a sand temperature of 1500 F which appeared a reasonable time-space average for the sand.

In view of the marked temperature dependency of the conductivity (see Fig. 24, Ref. 5) particularly for the sand 20 to 30 it was desired to compare the freezing process as described above with that obtained from a sand having conductivity varying with temperature. The temperatures change with time and with distance from the interface. In the experiments however, the time dependency was neglected and only the change with location was considered. It will be noted, that Experiments 1 and 4 refer to sand of the same density and the same average conductivity. Therefore the selection of conductivity values for Experiment 4 was based on the results of Experiment 1.

For each of the following distances from the interface a time temperature curve was drawn: 0.225, 0.475, 0.725, 1.05, 2.10 in. From each of the different curves the mean temperature (time average) was determined. These time averages then in turn were plotted against distance from the interface, and the appropriate conductivity values were taken from Lucks. This paper shows the conductivity values from 750 to 2250 F. The extrapolation is explained in the Appendix I.

The conductivity was highest at the interface and was lowered in steps towards the outside surface of the mold. The steps are shown in Fig. 5 of this report and should be read on the right ordinate scale. For example, the conductivity over a distance from 0.975 in. to 1.15 in. from the interface was 0.295 Btu/ft hr, F.

Experimental Results

The experiments were carried out in the same way as those previously described. The time required for different parts of the casting to solidify was observed and noted.

It should be remembered that the experiments on the analyzer show, for different parts of the casting, the time required to drop in temperature from the value at pouring to any other desired point. In previous presentations two curves were shown, the one indicating the time required to drop from the pouring temperature to the liquidus and the other the time required to drop from the pouring temperature to the solidus. It has been consistently observed that the times required according to the analyzer for any point in the casting to drop to the liquidus check very closely with the times of solidification obtained in bleeding tests.

In the present experiments again these two time curves were taken and in addition the temperatures in the sand were observed.

In Fig. 1, five curves are plotted. Times after pouring are shown as values on the abscissa axis (horizontal line) and solidified thickness values are shown as ordinates (vertical axis). The chart comprises five curves. One each for experiments 1 to 4; in addition the curve from Fig. 7 (Ref. 2) for "no air gap" is repeated for the sake of comparison. This curve is marked on the chart "old tests." The figure 0.9 indicates the constant sand conductivity used in that test.

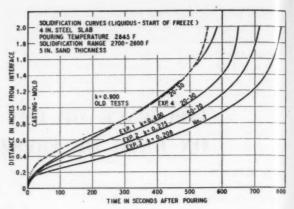


Fig. 1

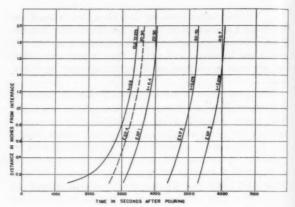


Fig. 2

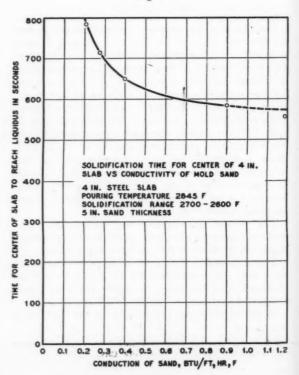


Fig. 3

In the earlier group of tests (Ref. 1 and 2) repeated experiments for the same condition—e.g. 4-in. slab—gave freezing times which were sometimes 10 per cent apart. Since that time the experimental technique has been greatly improved, so that the reproducibility of tests is much better. Here the most reliable looking curve (Ref. 2, Fig. 7) was selected for comparison.

The curves for Experiments 1 to 3 are labeled by the experiment number, the designation of the sand and the sand conductivity; the curve for Experiment 4 is labeled by the experiment number and the sand designation, the sand conductivity varying according to the distance from the interface.

Figure 2 shows similar curves, however the times are those required to reach the solidus point.

By comparing the different curves one can get some information regarding the influence of conductivity on solidification. The foundryman is most interested in the time required for the center of the casting to solidify, although for certain attempts of directional solidification the time required for freezing of a layer next to the surface may be of interest. For the latter case no presentation simpler than that of Fig. 1 and 2 seems possible and it is necessary to compare the different curves.

For total solidification however, a more direct comparison of the different sands may be had from Fig. 3, which was derived from Fig. 1.

In Fig. 3 the sand conductivity values are plotted on the abscissa axis (horizontal line). The times required for the center of the slab to drop from the pouring temperature to the liquidus point are plotted on the ordinate axis (vertical line).

This curve shows that with very low sand conductivities the solidification time increases quite considerably; as the sand conductivity increases its influence becomes less and less noticeable. The total influence of sand conductivity is relatively small. A change of conductivity from 0.2 to 0.9, or a 4.5-fold increase results in a drop of solidification time from 800 sec to 585 sec or a drop of 27 per cent.

These comments refer to the case of conductivity not varying with temperature. There is no accurate method to include in this comparison the Experiment 4 with its varying thermal conductivity. An approximate comparison may be had by plotting the conductivity next to the interface on the abscissa axis in Fig.

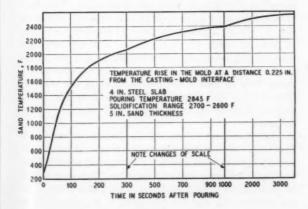


Fig. 4

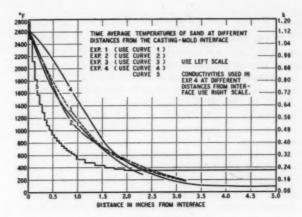


Fig. 5

3. It would be expected that Experiment 4 should yield a time value somewhat above that for the k value prevailing at the interface but this was not observed. Instead the point lies below the curve at the point which would hold if the k value at the interface (1.19) would exist throughout the sand.

Thus, possibly the curve should be lower and come closer to the value of 557 F at k = 1.19. The value for k = 0.9 was one of the first solidification experiments on the analyzer, and since the time of these experiments the technique of observations was improved, so that this time (k = 0.9; 585 sec) is too long.

Figure 4 gives one example for the sand temperature (vertical axis) plotted against time (horizontal axis) for a point 0.225 in. from the interface. Similar curves were drawn for several points and for each of these curves the mean temperature was determined. These mean temperatures (time averages) are plotted in Fig. 5. On the abscissa axis (horizontal line) the distances from the interface are plotted whereas the time average temperatures are plotted on the left ordinate scale (vertical line). Four curves are shown, one for each of the four experiments.

Although the average temperatures in the sand do not differ greatly with conductivity of the sand, the solidification times, as mentioned above, are distinctly different for the four cases which were investigated. It is interesting to note that the average sand temperatures in the case of Experiment 4 are somewhat higher than those in the three other experiments for most points between 0.1 to 1.5 in. from the interface; the interface temperatures however are close.

The theoretical interface temperature at the moment of casting can be calculated as explained in Appendix II. The values for this theoretical interface temperature at the moment of casting are shown in Table 2.

TABLE 2

| Experiment No. | Interf | mperature a face at Mom Pouring, F | ent |
|----------------|--------------|--|-----|
| 1 | 1000 | 2844 | |
| 2 2 | G c'a conte | 2598 | |
| 3 | and the | 2640 | |
| 20 4 35 1 | 7 mg - 1 - 1 | 2365 | |

Further Work

In the present experiments the change of conductivity with time was considered. Actually the computation should be made so that at each point in the computing circuit the conductivity is varied according to the temperature prevailing at this point at various times. In view of the relatively small change of solidification rate with change of sand conductivity such refinement in experimental procedure does not seem warranted.

All the experiments in this report and in previous reports of this laboratory have dealt with the case of dry sand. Actually the sand at the instance of pouring metal into the mold is not dry. Therefore an inquiry should be made to find out how the moisture in the sand influences the solidification rates.

APPENDIX I

Extrapolation of Thermal Conductivity

The curves of thermal conductivity shown by Lucks et al⁵ covers only a range from 750 to 2250 F.

The curve showing the temperatures (time averages) for Experiment 4 at different distances from the interface shows that temperatures occur from almost 2700 F to as low as 360 F.

The curve for the sand 20-30 (Lucks' designation) has to be estimated for higher temperatures. An analysis of Lucks' values shows that the conductivities change at higher temperatures almost with the fourth power of absolute temperatures (degrees Rankine). The higher the temperature becomes the better the fourth power relationship seems to hold.

Table 3 contains the various temperatures (column 1) and the conductivities (column 4). Column 3 shows the fourth powers of the absolute temperatures; the last column shows the ratio of fourth powers of temperature to conductivity.

For temperatures below 750 F the radiation between the sand particles has only a small effect and therefore the fourth power law probably does not hold. By ex-

TABLE 8

| | TABLE 0 | | | | | | |
|------------|----------|-----------|------------------------|----------------------------------|--|--|--|
| Tempe | rature | Ex | trapolation of k | values | | | |
| F | R | | k | | | | |
| Fahrenheit | Rankine* | R4 x 10-0 | Btu/sq ft,hr, F/in. | $\left(\frac{R}{100}\right)$ 1/4 | | | |
| 1 | 2 | 3 | 4 | 5 | | | |
| 2000 | 2460 | 3660 | 7.20ª | 508b | | | |
| 2100 | 2560 | 4300 | 7.72 | 557₺ | | | |
| 2200 | 2660 | 5010 | 8.46* | 592b | | | |
| 2250 | 2710 | 5400 | 8.93* | 605b | | | |
| 2300 | 2760 | 5806 | 9.414 | 617° | | | |
| 2400 | 2860 | 6690 | 10.534 | 635° | | | |
| 2500 | 2960 | 7680 | 11.82d | 649° | | | |
| 9600 | 2060 | 9770 | 18 994 | 658c | | | |

Values a read from Fig. 24, reference 5 Values b calculated from column 3 and 4 Values c extrapolated from values b Values d calculated from values c trapolation of the curves for temperature below 750 F the following values for conductivity at lower temperatures were estimated:

600 F: k = 0.237; 500 F: k = 0.229; 400 F: k = 0.222 (k in Btu/ft.hr,F)

For great distances from the interface the sand hardly starts to rise and one should conceivably work there with conductivities prevailing at temperatures only slightly above room temperature. The relatively small change of conductivity with temperature and the fact that the sand at such distances has relatively little bearing on the solidification of the casting were the reasons why no attempt was made to extrapolate the sand conductivities to lower temperatures than 400 F.

APPENDIX II

Calculation of Interface Temperature

Riemann and Weber⁶ give an equation for the interface temperatures prevailing when two semi-infinite slabs of different thermal properties and different initial temperatures come in sudden.contact. This equation is

$$t_{inter} = \frac{t_c \left(\sqrt{kcp}\right)_c + t_m \left(\sqrt{kcp}\right)_m}{\left(\sqrt{kcp}\right)_c + \left(\sqrt{kcp}\right)_m}$$

Here t designates temperatures, k thermal conductivities and cp volumetric specific heat. Subscript c refers to the casting and subscript m to the mold.

At the moment of casting and for a short time the thickness of mold and casting do not enter the picture; the bodies for different purposes may be considered to be of infinite thickness. In the first few moments the existence of heat of fusion and the change in thermal properties between liquid and solid state do not enter the picture. It is still possible to use Riemann's equation for calculation the temperature of the interface at the moment of pouring.

APPENDIX III

In view of the paper by Pellini and Bishop⁷ and the work of Ruddle⁸ it appears that the solidus lines called "end of freeze" are at least as important as the "liquidus points" which in previous work had been found to check well with bleeding tests. Therefore another graph, Figure 6, is presented here similar to Fig. 3. Figure 6 shows the time in seconds for center of slab to reach the solidus. It will be seen that whereas the times are considerably longer, the shape of the curve is similar to that in Fig. 3. There is a sharp drop in time if the conductivity of the sand increases from low values to approximately 0.4; but a further increase of conductivity is of relatively minor influence.

The experiments show the significance of sand conductivity. Within the customary range of conductivity, say between 0.5 and 1.0 a change of conductivity has little influence on solidification. But if it would be possible to obtain sands with very low conductivities between 0.2 and 0.5 Btu/sq ft, hr,F such sands together act as "negative chills."

^{*} The Rankine temperature scale is a scale of absolute temperature using the Fahrenheit degree. The Rankine temperature is thus 460 deg above the Fahrenheit temperature.

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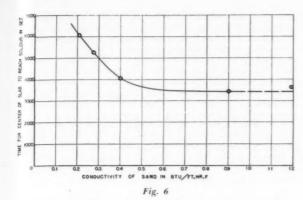
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DISCUSSION

Chairman: H. A. Schwartz, National Malleable & Steel Castings Co., Cleveland.

Co-Chairman: E. C. TROY, Foundry Engineer, Palmyra, N. J. CHARLES LOCKE:1 In regard to the use of thermal measurements for complete solidification, there is strong evidence from our work that increase in pouring temperature or superheat will affect the time for solidification in the directions brought out by B. H. Alexander.*

Use of the dumping or bleed method was found to be totally inadequate as a criterion for the way particular molding materials will change the rate of solidification. Much needs to be done in taking advantage of the properties of molding sands to control solidification. We look with interest to the work of Dr. Paschkis to find out what effect the change in thermal diffusivity and conductivity of the sand has on some of the other properties affecting the flow of heat and what effect it will have on solidification times

We know that Dr. Paschkis cannot give us answers unless he has numbers to put into the heat and mass flow analyzer. He has the opportunity to check a range of guesses to find out which one is correct. It seems to me for those who are doing work in this field that perhaps their work should be directed to some absolute measurement of properties so that Dr. Paschkis' analyzer can be used to its fullest extent.

S. L. GERTSMAN:2 Since we have always been interested in chills and high conductivity material we would appreciate some elaboration on the desirability of low conductivity sands.

Dr. Paschkis: Low conductivity sands could help in obtaining directional solidification. One could put low conductivity sands conceivably at some points which one wants to freeze slower than others. Low conductivity sand is a sort of negative chill.

W. S. PELLINI,3 and H. F. BISHOP4 (Written Discussion): The Heat and Mass Flow Analyzer Studies conducted by Dr. Paschkis under the auspices of the A.F.S. Heat Transfer Committee have been of great interest and aid to us in conducting thermal analyses of the solidification of sand and chill castings. Inasmuch as we have found surprising correspondence (surprising to us, not to Dr. Paschkis) between our experimental results and the predictions of Dr. Paschkis' Analogue Analyzer we wish to take this opportunity to select several items from our accumulated data which may provide indications of the probable correct average values of sand conductivity, of the several assumed values experimented with on the Analyzer. As Dr. Paschkis points out sand conductivity is a variable which changes at any fixed location in the mold as the temperature of the sand is raised during the solidification cycle. There are of course other inherent determining factors such as grain and density

The following provides a listing of the hypothetical casting conditions assumed by Dr. Paschkis in comparison to the actual test conditions for the Naval Research Laboratory tests:

| | Analogue * Assumptions | NRL Conditions |
|---------------------|---------------------------|-----------------------------------|
| Sand Grain Size | Various | No. 80 A.F.S. |
| " Binder | None | 4% Bentonite 1% Mogul |
| " Water Con- | | |
| tent, % | None | 3.2 |
| " Density, lb/ | | |
| cu ft | Various | 102 |
| " Wall Thick- | | |
| ness, in. | 5 | 8 |
| " Thermal Con | | |
| ductivity | Various | Not determined |
| Casting Solidifica- | 2700-2600 F | 2700-2575 (0.60%C) |
| " Shape | (approx. 0.60%C steel) | Also 2750-2660F (0.42%C) |
| | Infinite 4-in. Plate | Measured at In- finite Plate |
| | | Position for 4-in. |
| | | Also 7x7-in. ingot 22-in. high |
| " V/S Ratio | 2 | 2 (slab) (1.75) ingot |
| " Pouring Temp. | 2845 F | 2800 F |

Thermal measurements were made at various positions from the mold-metal interface into the sand and also into the metal. Pt-Pt Rh thermocouples were used to determine temperatures. The thermocouple wires were protected by fused quartz tubing of 1/32-in. wall thickness. Temperatures were recorded on rapid multi-point potentiometer-recorders. Liquidus and solidus points were determined by inverse rate curves.

Figure A presents the results of several tests plotted together with Dr. Paschkis' curve (his Fig. 4) which shows the temperaturetime relationships at 0.25 in. from the metal-mold interface (Paschkis' curves are for 0.225 in., the differences being negligible and outside our absolute accuracy of location). It is noted that the NRL 4-in. plate test shows a striking similarity in form and position to the theoretical curve with the exception of a somewhat lower (200 F) general temperature level. It is not clear if Dr. Paschkis intends to indicate maximum temperature at the point he has chosen to end his curve, if he does we must conclude that this point is verified within 100 sec. The 7x7-in. ingot curves (0.60 and 0.42 per cent C) have been included inasmuch as these show close check after the first 1000 sec to the 4-in. plate casting curves and also because of the deviation dur-

¹ Supervisor, Foundry Process Research, Armour Research Foundation, Chicago.

² Head, Steel and Steel Castings Section, Bureau of Mines, Canada.

See "Solidification of Ingots" by B. H. Alexander, p. 270.

⁸ Head, and ⁴ Metallurgist, Metal Processing Branch, Metallurgy Div., Naval Research Laboratory, Washington, D. C.

For methods used to determine conditions refer to "The Contribution of Riser and Chill Edge Effect to Soundness of Cast Steel Plates" by H. F. Bishop and W. S. Pellini in this volume, p. 185.

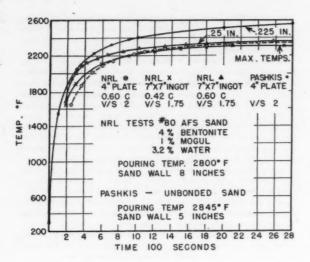


Fig. A

ing the first 1000 sec which may be due either to the form factor of the 7x7 -in. castings or the somewhat different V/S ratios. We would appreciate Dr. Paschkis' comments on this point. We know from his work, and also from tests of our own, that the start of solidification is indeed a function of the form factor at same V/S ratios even though the end of solidification is not affected. We note also that during this initial interval our experimental ingot curves closely approach the Analogue curve.

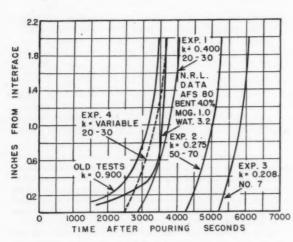


Fig. B

Figure B places the experimental "end of solidification" curve, determined by noting the time to reach the solidus temperature, on the Analogue solidification curves (his Fig. 2) predicated by assumptions of various average conductivities. It is noted that the NRL end of solidification curve matches closely the curve for 0.400 BTU/ft, hr., °F, conductivity with the exception of the initial solidification region for which experiments show a decided advance in position more like the Analogue curve for K \pm .900. It is also noted that the NRL curves obtained using a No. 80 A.F.S. sand fall in the locations of the coarser screened 20 to 30 sands. This is probably the results of binder and $\rm H_2O$ additions and/or grain size distribution differences. It interests us to know why the "old" test curve assuming K \pm .900 has been drawn with asymptotic approach while the present experiments show otherwise. These last are of course in keeping with the displaced

parabolic relationships postulated by Field. From foundry observations of the rapidity of initial skin formation we would rather favor the correctness of the asymptotic approach curve.

Inasmuch as the Analogue curves No. 4 and No. 1 (Fig. 2) are quite close together and the experimental curve falls be tween the two it is not possible to deduce if the temperature corrected No. 4 curve provides a better fit with experiment. It may be concluded, however, that average values of .400 are approximately correct as Analogue constants, at least for the casting conditions used in the NRL experiment.

Dr. Paschkis is to be congratulated on another of an exceedingly fruitful series of papers. Careful study of this series should be most rewarding to those making the effort. Assuredly, the series requires careful study due to the large amount of information presented. It is hoped that the A.F.S. Heat Transfer Committee will find it possible to continue this fine work.

Dr. Paschkis (Reply to Messrs. Pellini and Bishop): I want to thank Dr. Pellini for his very kind words regarding our work.

His Fig. A calls for comment from two points. First, he points out the difference between his curve "NRL 4-in. plate" and the Columbia curve. The difference is certainly in part explained by the difference in distance from the interface. As may be seen from Fig. 5 of the article, a difference of 0.025 in. can account for 50 of the roughly 200 degrees. The balance can be possibly accounted for by different thermal conductivity of the sand but also by errors in temperature measurements at NRL. If the head of the thermocouple finds itself in relatively poor contact with a solid sand particle, a delay may be big. One technique to overcome this is, to attach a small disc to the bead of the thermocouple; but care must be taken that the disc is parallel to the interface so that it finds itself entirely in an isothermal plane.

The second point regarding Fig. A is the intersection of the curve marked "7x7-in. ingot" and that marked "4-in. plate." The reason for this intersection is not obvious and I think it would be desirable if Dr. Pellini could explain more fully what his reasoning is by attributing the intersection to the shape factor.

Regarding his Fig. B: he observes correctly that his observed data match in part closely to our curve "k = 0.900", and in later times our curve "k = variable, 20-30." One explanation for this shift might be that both our curves in this report hold for dry sand. At least at early times the moisture of the sand will greatly influence the apparent conductivity. However the time of roughly 1000 or 1500 sec at which Pellini's curve hugs the "k = 0.900" curve is so long that I would not be too sure about the moisture influence. Conceivably the conductivity values for "k = variable 20-30" should undergo a different temperature function than that assumed.

Regarding the question of the different approach of the various curves of my Fig. 2 to the abscissa axis, it should be noted that in the computer experiments we measure temperatures not directly at the interface but a small distance away from the interface. The extension of the curves to "distance = 0" is incorrect. Figure 2 ends at the point of the last observation. Even so, the "old test" curve seems to bend more sharply than any of the four other curves. But it is conceivable that these curves still would bend between the last observed point and zero distance.

Mr. Locke is correct in stating that we cannot produce curves on the analyzer unless we know the thermal properties underlying the process. However I believe that it is one of the advantages of any computation and also by means of the analyzer that one can explore the relative influence of various variables. For example in the case of sand, I think it would have been a wasted effort on the part of any physicist, foundryman or engineer to try to develop a sand with particularly high conductivity. Our curves show that high conductivity sands beyond a certain value do not influence the freezing greatly. On the other side, such curves show that it might be worthwhile to make such efforts to get low conductivity sand.

I think it should not be forgotten that one advantage of any computational technique is that one can study properties without referring to actual availability of material and without interference of other properties which might be changed inadvertently in actual experimentation.

IMPROVED TEST BARS FOR STANDARD AND DUCTILE GRADES OF CAST IRON

By

Richard A. Flinn* and R. Wayne Kraft*

ABSTRACT

Two principal difficulties are encountered in the present ASTM Standard A-48 arbitration bars for gray cast iron: shrinkage and narrow range of cooling rates. The presence of shrinkage handicaps the producer most severely in the highest strength, section sensitive materials such as the ductile or nodular cast irons and the narrow range of cooling rates is a handicap in all

Accordingly, after several unsuccessful attempts with the present specifications, a series of Y-shaped test blocks was designed to provide sound test bars in a wider range of section sizes (1/4 in. to 3 in.). Comparative tests were made of Y-blocks and arbitration bars in four materials: pearlitic ductile iron, ferritic ductile iron, Class 50 gray iron and Class 30 gray iron.

All the arbitration bars and none of the Y-blocks exhibited shrinkage in the test sections. This results from a lower thermal gradient 3 to 9 F per in. in the arbitration bars compared with 38 to 61 F per in. in the Y-blocks. The difference in mechanical properties was severe in the pearlitic and ferritic ductile irons and negligible in the Class 50 and Class 30 irons. The shrinkage effect could be shown, however, in the lower strength materials by 0.505 in. diam specimens cut from the center of the larger arbitration bars.

The Y-block series provides four times the range of cooling rates of the arbitration bars and, consequently, presents a better evaluation of mechanical properties in all the materials.

An improved transverse test technique was developed which eliminates the errors due to apparatus and cast surface inherent in the present method.

The Y-block tests are recommended to the appropriate A.F.S. and ASTM committees for consideration as an alternate stand-

History

THE COMMITTEE ON GRAY IRON TEST BAR DESIGN** was formed in 1946 in response to many requests to the A.F.S. and ASTM for improvement of the ASTM Standard for Gray Iron test bars. A survey of the prob-

lem showed that the principal difficulties with the present arbitration bars were (1) unsoundness and (2) limited representation of casting section size. It was also established that the prime function of the test bar was to provide a sound tensile specimen, and that transverse properties were secondary in the general specification (A-48).

The recent developments in nodular or ductile cast iron have accentuated the need for an improved standard; investigators in this new field have been forced to employ the standard keel block used for steel castings in order to obtain sound bars. As an example of the unsoundness caused by using a long narrow bar, the original British announcements of nodular cast iron claimed only a material that was better than cast iron, but inferior to malleable iron. The low ductility was probably the result of insufficient feeding and not of the metal structure.

Objective

Several years before the announcement of ductile cast iron, however, the Committee believed that a new standard was necessary for the entire field of cast iron and set up the following objective:

'To develop complete specifications for a group of tensile test bars which shall be sound and representative of a wider range of cooling rates. Transverse test technique, although optional, shall require sound, machined specimens."

The experimental work may be reviewed in two sections, first, the early experiments, and second, the Y-block investigations.

Early Experiments

Investigations of the Present Standard-Long before the organization of the Committee, many attempts had been made to develop a procedure for obtaining a sound arbitration bar of the present design. It is not the purpose of this report to review or attempt to evaluate all these results because they indicate, in general, that the arbitration bar is basically an extremely difficult casting to feed properly. The only exceptions are the lower strength irons which have a large amount of graphitization to compensate for shrinkage. As far back as 1922, Moldenke¹ reported a

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^{**} The membership of the A.F.S. Committee on Gray Iron Test Bar Design is as follows:

A. P. Gagnebin, International Nickel Co., New York.

W. W. Levi, Lynchburg Foundry Co., Lynchburg, Va. H. W. Lownie, Battelle Memorial Institute, Columbus, Ohio.

H. W. STUART, U. S. Pipe and Foundry Co., Burlington, N.J.

H. F. TAYLOR, Vice Chairman, Massachusetts Institute of Technology, Cambridge, Mass.

R. A. Flinn, Chairman, American Brake Shoe Co., Mahwah, N. J.

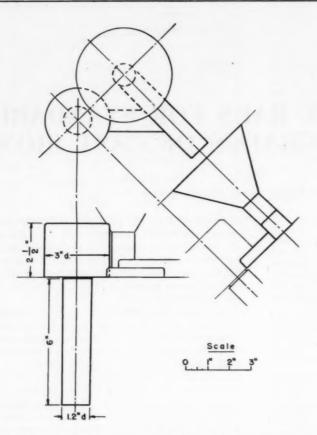
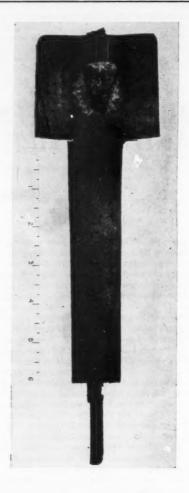


Fig. 1 (Above)—Gating of early test bar casting.

Fig. 2 (Right)—Acid etched cross-section of 1.2-in. diam by 6-in. long bar of low carbon iron.



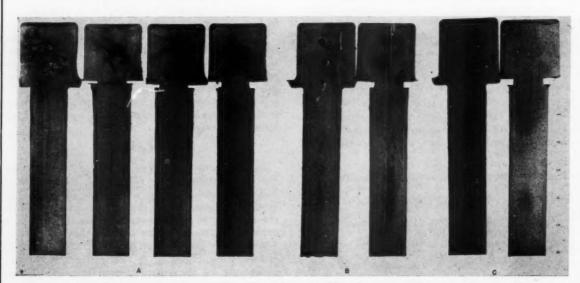


Fig. 3-Acid etched cross-sections of 1.2-in. diam by 6-in. long bars of white iron (A), austenitic gray iron (B), and Class 30 iron (C).

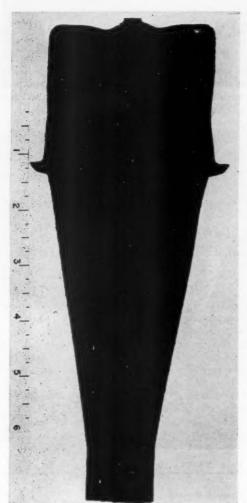


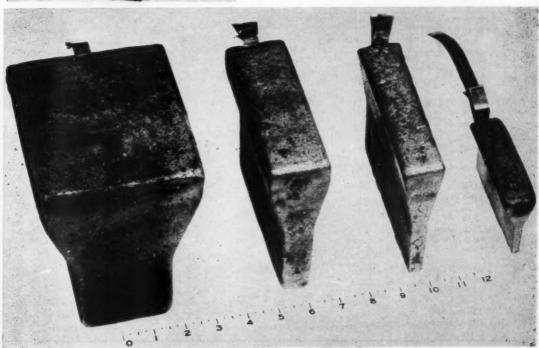
Fig. 4 (Left)—Acid etched cross-section of nominal 1.2-in. diam by 6-in. long bar of low-carbon iron.

high percentage of flawed transverse test bars when using a design similar to the present standard. In 1945, Taylor² used X-ray and deep etching methods to demonstrate the presence of fine shrinkage in a number of bars that would be considered sound by visual examination. Schaum³ then carried out a series of experiments in moderate strength iron, in an attempt to produce sound bars, using a wide variety of gating and risering techniques. Many of the bars were apparently sound by visual inspection, but when subjected to X-ray examination only one gating technique was found satisfactory. Since this involved mold in version and a complicated gating system, it was considered beyond the range of commercial specifications.

At the conclusion of Schaum's experiments, the Committee decided that the basic trouble was the geometry of the test bar which was in turn necessitated by the long span of the standard transverse test. Since the tensile test and not the transverse test is the basis of the general A-48 ASTM specification for gray iron, future work was directed towards obtaining first a sound tensile bar, from which a transverse specimen could then be machined if needed. This permitted the use of a smaller length to diameter ratio and simplified risering. In addition, the wide fluctuation in transverse data caused by surface imperfections could be avoided by the use of sound machined bars.

Vertically Poured $\frac{1}{2}$, 1.2, 2-in. Bars—A set of patterns was designed to produce three sizes of test bars: $\frac{1}{2}$ in. diam x 3 in. long, 1.2 in. diam x 6 in. long and 3 in. diam x 6 in. long. The type of gating and risering

Fig. 5 (Below)-1/4-in., 1/2-in., 1-in. and 3-in. Y-blocks before sectioning.



and photographs of the test bars are shown in Fig. 1, 2 and 3. These bars were all gated at one end to provide a simple cutoff practice.

The most difficult of these designs was the 1.2 in. bar. It was not possible to develop a sufficient thermal gradient to provide a sound straight bar by end gating, even using a large riser. Finally, pronounced tapering was found necessary (Fig. 4).

Since this resulted in a wedge-shaped specimen that

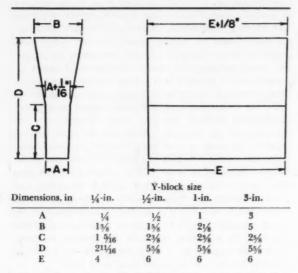


Fig. 6-Dimensions of 1/4-in., 1/2-in., 1-in. and 3-in Y-blocks.

was no longer a 1.2-in. bar, it was decided to use side feeding as described in the next series of experiments.

The Y-Block Experiments

Background—For the past nine years a series of Y-blocks has been used in this Company to provide sound test material in a wide variety of analyses of gray irons, white irons and steels, Fig. 5 and 6. The design of these blocks is essentially a short plate section adequately fed by a large head. In principle, the castings resemble the steel keel block. The advantages of the Y-blocks are the availability of a wider range of section thicknesses (1/4 in. to 6 in.) and easier handling and layout in cutoff.

This casting was reviewed earlier in the investigation, but it was decided to try first the designs requiring less cutoff. After the failure of these earlier attempts, Y-blocks were tested in four widely different grades of cast iron, as discussed in the following sections.

Y-Block Procedure

Production of Castings and Specimens—Two of the heats were commercial cupola heats of Class 30 and Class 50 irons and the third and fourth heats were induction melted ductile cast iron made in our Experimental Foundry. One heat was heat treated to produce a ferritic structure, as discussed later. Analyses are given in Table I.

The molds of Y-blocks and arbitration bars were made of the following mix and baked at 425 F for 3 hr. The mix is designed for high dry strength and medium collapsibility.

TABLE 1-ANALYSES AND POURING TEMPERATURE OF CASTINGS

| Iron | Casting | T.C. | Mn | P | Chemical S | Analysis, Si | Per cent Mo | Mg | Cu | CC | Pouring Temp. |
|-----------|----------------|------|------|-------|---------------|-----------------|----------------|-------|------|-----------|------------------|
| Pearlitic | 1/4-in. Y | 3.69 | 0.10 | 0.017 | 0.015 | 2.95 | | 0.103 | 0.94 | 0.69 | 2460 |
| Ductile | 1/2-in. Y | | | | | | | | | 0.94 | 2405 |
| | Î-in. Y | | | | | | | | | 0.69 | 2435 |
| | 3-in. Y | | | | | | | | | 0.86 | 2460 |
| | 0.875-in. diam | | | | | | | | | 0.83 | 2505 |
| | 1.2-in. diam | | | | | | | | | 0.77 0.91 | 2510 |
| | 2.0-in. diam | | | | | | | | | 0.82 | 2515 |
| Ferritic | 1/4-in. Y | 3.58 | 0.12 | 0.010 | 0.015 | 2.91 | | 0.071 | 0.67 | | * 2610 |
| Ductile | 1/2-in. Y | | | | | | | | | | 7 2600 |
| | 1-in. Y | | | | | | | | | | 2565 |
| | 3-in. Y | | | | | | | | | | 2560 |
| | 0.875-in. diam | | | | | | | | | | 2510 |
| | 1.2-in. diam | | | | | | | | | | 2500 |
| | 2.0-in. diam | | | | | | | | | | 2495 |
| Class 50 | 1/4-in. Y | 2.96 | 1.05 | 0.114 | 0.072 | 1.63 | 0.67 | | | 0.83 | 2540 |
| | 1/2-in. Y | | | | | | | | | _ | 2510 |
| | 1-in. Y | | | | | | | | | 0.77 | 2500 |
| | 3-in. Y | | | | | | | | | 0.78 | 2475 |
| | 0.875-in. diam | | | | | | | | | 1.39 | 2535 |
| | 1.2-in. diam | | | | | | | | | 0.86 | 2535 |
| | 2.0-in. diam | | | | | | | | | 0.87 | 2555 |
| Class 30 | 1/4-in. Y | 3.40 | 0.71 | 0.423 | 0.152 | 2.38 | | | | 0.57 | 2650 |
| | 1/2-in. Y | | | | | | | | | 0.69 | 2635 |
| | 1-in. Y | | | | | | | | | 0.66 | 2625 |
| | 3-in. Y | | | | | | | | | 0.65 | 2610 |
| | 0.875-in. diam | | | | | | | | | 0.67 | 2495 |
| | 1.2-in. diám | | | | | | | | | 0.74 | 2490 |
| | 2.0-in. diam | | | | | | | | | 0.75 | 2490 |

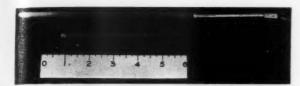


Fig. 7-Assembly of large and small thermocouples.



Fig. 8-Location of thermocouples in 1/4-in. Y-block.

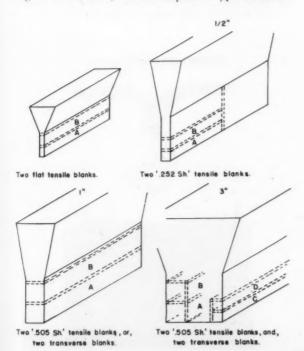


Fig. 9-Sectioning procedure for Y-blocks.

800 lb No. 30 N. J. Silica Sand 12 qt Core Oil
125 lb Silica Flour 8 gal Water
6 lb Mogul
6 lb Kordek
Moisture, 4.2 per cent Green Strength, 1.8 lb
Permeability, 99 Tensile Strength, 375 lb

All the molds for a given heat were poured from the same ladle. Pouring temperatures are given in Table 1, and are comparable for both arbitration bars and Y-blocks.

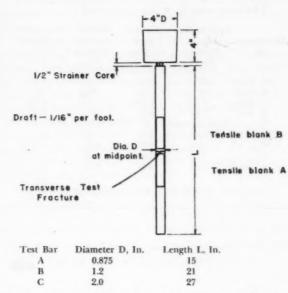


Fig. 10-Gating, dimension and sectioning of arbitration bars.

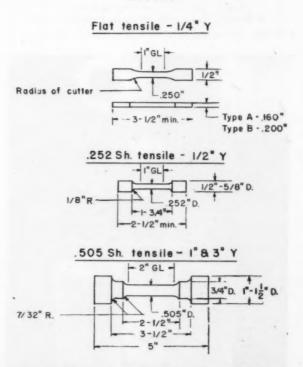
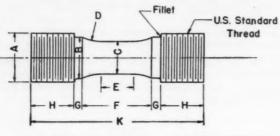


Fig. 11-Dimensions of tensile specimens from Y-blocks.

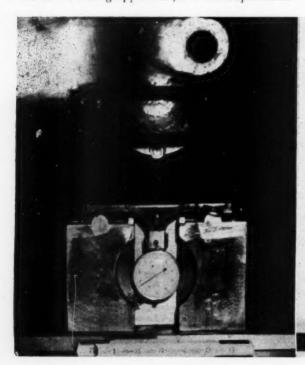


| Dimensions, In. | | 1.2-in. Bar Type B (0.750 in. Th.) | Type B Modified (0.798 in. Th.) | 2.0-in. Ba Type C (1.25 in. Th.) |
|--------------------|----------------|---|--|---|
| A, diam | 3/4 | 11/8 | 11/8 | 17/8 |
| B, diam | 5/8 | 15/16 | 15/16 | 17/16 |
| C, diam | 0.505 | 0.750 | 0.798 | 1.250 |
| D, rad | 1.00 | 1.00 | 1.00 | 2.00 |
| E | Shall be equal | to or great | er than C (| diam). |
| F, min | 1.25 | 1.50 | 1.50 | 21/4 |
| G | 0.25 | 0.25 | 0.25 | 5/16 |
| H | 1.00 | 1.00 | 1.00 | 13/4 |
| K, min | 3.75 | 4.00 | 4.00 | 63/8 |

Fig. 12-ASTM tensile specimens (A 48-46).

Pig. 15-Acid etched cross-sections of Class 50 iron tensile specimens and 1/4-in. Y-blocks.

Fig. 13 (Left)—Transverse testing apparatus with special deflection gage, and (Right)—Exploded view of transverse testing apparatus for Y-block specimens.



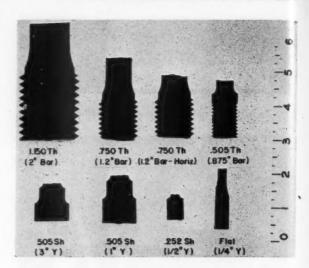
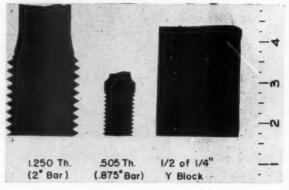
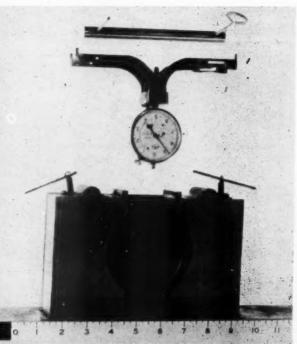


Fig. 14-Acid etched cross-section of pearlitic ductile iron tensile specimens.





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Yield & Tens

Percent

E Sec In

Platinum-10% rhodium platinum thermocouples were encased suitably in fused silica tubes as shown in Fig. 7; very light tubes were used for the smaller sections to minimize the cooling effects. The thermocouples were located to obtain the cooling rate of the test bar portions, and also to provide quantitative data regarding the temperature gradient existing in the different designs. A photograph of a 1/4-in. Y-block with thermocouples is shown in Fig. 8.

The castings were cooled in the mold to below 200

F and sectioned as shown in Fig. 9 and 10.

To investigate the properties of ductile iron with a ferritic matrix, one heat of pearlitic ductile iron test bars was treated as follows: 1650 F-20 hr; Furnace Cooled at 200 F/hr to 1275 F; held 4 hr; Furnace Cooled to room temperature. This treatment was longer than necessary, but insured the absence of massive carbide in the lighter sections. Ferritic ductile iron can, of course, also be produced as cast, but for this comparative study a uniform matrix was desired.

Test specimens were machined as illustrated in Fig.

11 and 12.

In all cases representative cross sections of the Y-blocks and arbitration bars were deep acid etched with 1:1 HCl at 160 F for 15 min (Fig. 14 and 15).

Mechanical Testing—Except in the case of the large tensile specimens from the 2-in. arbitration bars, a Baldwin Southwark 60,000-lb capacity testing machine was employed. SR-4 resistance wire strain gages were used to measure elongation and modulus of elasticity. Since the ASTM standard 1.25-in. diam tensile specimens of ductile iron were beyond the capacity of the laboratory machine, a 1.15-in. diam specimen was machined in order to be within the capacity of the 100,000-lb machines of two other testing laboratories. However, in both cases the grips, rather than the specimen, were broken. Smaller specimens were then machined from the remaining material as noted in Table 2.

The use of 0.505-in. diam specimens from the larger Y-blocks is in accordance with present ASTM A-27 specimen practice for cast steel.

To obtain the properties of the lighter section Y-blocks, a flat tensile coupon was used with Templin grips. The properties of this specimen have been shown on several occasions to lie within 5 per cent of the tensile values of the 0.505-in. diam design by comparable specimens of both types cut from 1-in. Y-sections of pearlitic ductile iron.

Transverse tests of the arbitration bars were made in the usual manner. The 2-in. diam bars and one of 1.2-in. bars of ductile pearlitic iron could not be broken in the available apparatus.

Transverse specimens, 6 in. long and ½ in. in diam, were machined from 1-in. and 3-in. Y-blocks and tested on a 5-in. span. A special dial indicating device using the principle of MacKenzie and Donoho⁴ was built to record deflections without errors due to crushing at the contact points, and extraneous movements in the

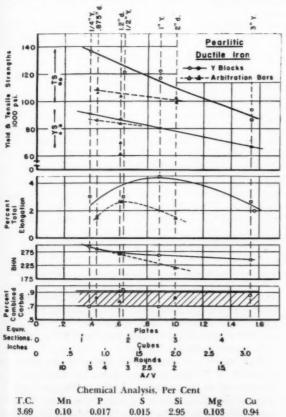
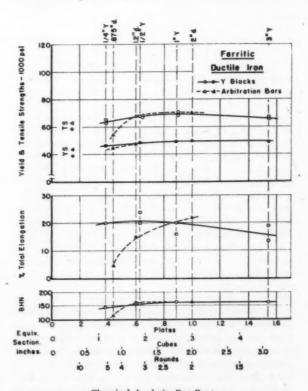


Fig. 16—Mechanical properties of pearlitic ductile iron.



Chemical Analysis, Per Cent
T.C. Mn P S Si Mg Cu
3.58 0.12 0.010 0.015 2.91 0.071 0.67
Heat Treatment: 1650 - 20 - F.C. to 1275 - 4 - F.C.

Fig. 17-Mechanical properties of ferritic ductile iron.

TABLE 2-MECHANICAL PROPERTIES OF DUCTILE, CLASS 50, AND CLASS 30 IRON

| | | | | Tensile | | | r Cent | Tangent | |
|-------------------|----------------------------|-----------------------------------|-------------------------------|------------------|---|------------|--------------------|--------------------------------|-----|
| Iron | Casting | Specimen Location ¹ | Type Specimen ² | Strength, psi | Strength, psi | Total | ngation Plastic | Modulus 10 ⁶ psi | BHN |
| Pearlitic Ductile | 0.875-in. Arb. Bar | A | 0.505 Th. | 106.750 | 86,300 | 1.61* | | 24.6 | 286 |
| | 0.875-in. Arb. Bar | A | 0.505 Th. | 109,500 | | 1.5 | | | |
| | 1.2-in. Arb. Bar | A | 0.705 Th. | 100,420 | 83,500 | 2.7 | | 24.3 | 269 |
| | | В | 0.505 Sh. | 91,500 | | 1.0 | | | |
| | 1.2-in. Arb. Bar | A | 0.505 Sh. | 92,750 | * | 0.5 | | | |
| | | В | 0.750 Th. | 87,200 | | 2.7 | | | |
| | 1.2-in. Arb. Bar | A | 0.750 Th. | 67,700 | 61,400 | _ | | 21.5 | 269 |
| | Horizontally Poured | В | 0.505 Sh. | 91,000 | | 1.0 | | | |
| | 2-in. Arb. Bar | A | 1.150 Th.4 | Could not | be broken. | _ | | - | 217 |
| | | В | 0.505 Sh. | 86,000 | | 1.5 | | | |
| | | Stub | 0.798 Th. | 103,000 | | 1.5 | | | |
| | 2-in. Arb. Bar | A | 0.505 Sh. | 99,500 | | 2.0 | | | |
| | 4-1 | В | 1.150 Th.4 | Could not | be broken. | | | | |
| | 1/4-in. Y | A | Sand inclus | | | | | | |
| | /4 | В | Flat tensile | | 91,000 | 3.0 | | 24.6 | 293 |
| | 1/2-in. Y | A | Sand inclus | | | | | | |
| | 72 | В | 0.252 Sh. | 121,400 | | 3.0 | | | 269 |
| | 1-in Y | A | 0.505 Sh. | 122,250 | 80,200 | Not ob | tained. | 25.4 | 262 |
| | | В | 0.505 Sh. | 117,400 | | 4.5 | | | |
| | 3-in. Y | A | 0.505 Sh. | 94,000 | 66,900 | 2.0 | | 25.5 | 248 |
| | J-111. 1 | В | 0.505 Sh. | 87,000 | 00,000 | 2.0 | | | |
| | | . C ⁵ | 0.797 Th. | 85,200 | | 2.7 | | | |
| Ferritic Ductile | 0.875-in. Arb. Bar | A | 0.505 Th. | 54,500 | 44,600 | 4.5 | | 21.2 | 116 |
| retiffic Ductife | 0.075-III. 711 D. Dai | В | 0.252 Sh. | 34,000 | 11,000 | 0.0 | | | |
| | 1.2-in. Arb. Bar | A | 0.750 Th. | 68,200 | 47,600 | 15.0 | | 22.8 | 163 |
| | 1.4 111. 7110. Dat | В | 0.505 Sh. | 64,750 | 11,000 | 9.0 | | - | 200 |
| | 2.0-in. Arb. Bar | Stub | 0.797 Th. | 70,700 | 50,100 | 22.0 | | 23.0 | 163 |
| | 4.0-111. 711 U. Dai | В | 0.505 Sh. | 63,000 | 30,100 | 7.5 | | 20.0 | 100 |
| | 1/4-in. Y | A | Flat | 62,500 | 46,400 | 6 | | 23.6 | 143 |
| | 1/4-111. 1 | В | Flat | 65,500 | 10,100 | 20.0 | | 4,5.0 | 110 |
| | 1/2-in. Y | A | 0.252 Sh. | 67,200 | 48,400 | 20.0 | | 22.2 | 156 |
| | 1/2-111. 1 | В | 0.252 Sh. | 67,800 | 10,100 | 24.0 | | dis dip mile | 150 |
| | 1-in. Y | A | 0.505 Sh. | 70,500 | 49,200 | 16.0 | | 23.7 | 163 |
| | 1-111. 1 | В | 0.505 Sh. | 68,000 | 75,400 | 20.0 | | 40.1 | 103 |
| | 3-in. Y | A | 0.505 Sh. | 67,000 | 48,900 | 19.0 | | 22.8 | 163 |
| | 3-III. I | В | 0.505 Sh. | 65,750 | 40,500 | 13.5 | | India AU | 103 |
| | | ь | 0.303 311. | 03,730 | | 13.3 | | | |
| Class 50 | 0.875-in. Arb. Bar | A | 0.505 Th. | Sand inc | lusion. | | | | 302 |
| 2000 | 0.075 111.7110. 1011 | В | 0.505 Sh. | 36,000° | ,,,,,,,,,,,,,,,,,,,,,,,,,,,,,,,,,,,,,,, | | | | |
| | 0.875-in. Arb. Bar | A | 0.505 Th. | 35,500° | None | 0.198 | None | 17.5 | |
| | 1.2-in. Arb. Bar | A | 0.750 Th. | 62,400 | | | | -110 | 269 |
| | 1.4-111. 2110. 1901 | В | 0.505 Sh. | 60,000 | | | | | 200 |
| | 2.0-in. Arb. Bar | A | 1.250 Th. | 54,100° | | _ | | 20.5 | 255 |
| | 4.0-m. A10. Dai | В . | 0.505 Sh. | 47,500 | | _ | | 40.0 | 200 |
| | 2.0-in. Arb. Bar | A | 0.505 Sh. | 50,250 | 48,000 | 0.53^{3} | 0.14 | 19.5 | |
| | 2.0-111 110. Dai | В | 1.250 Th. | 54,500 | 10,000 | 0.55 | 0.11 | 13.3 | |
| | 1/4-in. Y | A | Too hard to i | | e Fig. 15 | _ | | | 302 |
| | 74-111. 1 | B | Flat | 65,100 | 115. 13. | | | | 504 |
| | 1/2-in. Y | A | 0.252 Sh. | 62,000 | None | 0.318 | 0.04 | 23.3 | 262 |
| | 72-111. 1 | B | 0.252 Sh. | 60,000 | HOME | 0.01 | 0.01 | 20.0 | 404 |
| | 1 : V | | | | 47 900 | 0 503 | 0.99 | 90.9 | 241 |
| | 1-in. Y | A | 0.505 Sh. | 52,500 | 47,300 | 0.59^{8} | 0.33 | 20.2 | 411 |
| | 9 to V | В | 0.505 Sh. | 50,500 | 4E 700 | 0.5 | 0 50 | 01.7 | 006 |
| | 3-in. Y | A | 0.505 Sh. | 54,500 | 45,700 | 0.90^{8} | 0.58 | 21.7 | 226 |
| | | B | 0.505 Sh. | 49,500 | | _ | | | |
| | | C_2 | 0.797 Th. | 51,400 | | - | | | |

testing machine, Fig. 13. See appendix.

To determine the errors in the conventional method of measuring the deflection between the cross head and the bed plate, a hardened ground tool steel rod 6 in. long and 0.500-in. diam was tested. The specimen was loaded twice to eliminate plastic deformation and then the deflection curve was taken by both methods. The cross-head deflection method gave erratic results because of deformation at the contact points and slippage between the cross-head and the loading screw. Errors of 30 per cent in both modulus and load were obtained. The device developed by

MacKenzie and Donoho was, therefore, modified as shown in Fig. 13 and used with a shockproof Ames gage for all transverse tests on the machined bars. The transverse data are discussed later under the subhead *Transverse Testing*.

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The numerical data for all the tests are given in Tables 2, 3, 4, 5 and 6. The graphs, Fig. 16, 17, 18 and 19, provide a comparison of arbitration bar and Y-block data. The area to volume ratio of all the castings, which is proportional to the cooling rate, is used as the basis of plotting the section size effects. This principle is discussed further in the section on range

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TABLE 2-CONTINUED

| | | - | Specimen | Туре | Tensile Strength, | 0.2% Yield Strength, | Elon | Cent gation | Tangent Modulus | |
|----------|----|--------------------|-----------------------|-----------------------|----------------------|-------------------------|-------|-------------------|--------------------|-----|
| Iro | on | Casting | Location ¹ | Specimen ² | psi | psi | Total | Plastic | 10° psi | BHN |
| Class 30 | | 0.875-in. Arb. Bar | A | 0.505 Th. | 33,500 | 32,800 | 0.52 | 0.238 | 13.7 | 212 |
| | | | В | 0.505 Sh. | 32,500 | | _ | | | |
| | | 0.875-in. Arb. Bar | A | 0.505 Sh. | 38,500 | 33,600 | 0.74 | 0.418 | 13.6 | |
| | | | В | 0.505 Th. | 34,000 | | | | | |
| | | 1.2-in. Arb. Bar | A | 0.750 Th. | 33,925 | 29,760 | 0.73 | 0.418 | 12.0 | 207 |
| | | | В | 0.505 Sh. | 28,250 | | _ | | | |
| | | 1.2-in. Arb. Bar | A | 0.505 Sh. | 29,500 | 27,000 | 0.56 | 0.26^{a} | 14.2 | |
| | | | В | 0.750 Th. | 33,450 | | _ | | | |
| | | 2.0-in. Arb. Bar | A | 1.250 Th. | 26,620 | 23,500 | 0.58 | 0.26^{8} | 10.6 | 207 |
| | | | В | 0.505 Sh. | 21,500 | | - | | | |
| | | 2.0-in. Arb. Bar | A | 0.505 Sh. | 23,000 | 19,500 | 0.54 | 0.178 | 14.8 | |
| | | | В | 1.250 Th. | 26,200 | | | | | |
| | | 1/4-in. Y | A | Flat | 41,000 | | _ | | | 217 |
| | | | В | Flat | 38,900 | | - | | | |
| | | 1/2-in. Y | A | 0.252 Sh. | 32,000 | 31,600 | 0.44 | 0.10^{8} | 13.7 | 187 |
| | | | В | 0.252 Sh. | 32,800 | | - | | | |
| | | 1-in. Y | A | 0.505 Sh. | 29,000 | 26,000 | 0.60 | 0.18 ^a | 13.6 | 174 |
| | | | В . | 0.505 Sh. | 26,000 | | - | | | |
| | | . 3-in. Y | A | 0.505 Sh. | 25,000 | 22,400 | 0.55 | 0.198 | 15.5 | 149 |
| | | | В | 0.505 Sh. | 23,000 | | 0.5 | | | |
| | | | C_2 | 0.797 Th. | 25,100 | | _ | | | |

^{&#}x27;See Fig. 9 for Y-blocks, Fig. 10 for arbitration bars.

of cooling rates.

Photomicrographs showing the type and distribution of graphite in the $\frac{1}{2}$ and 3-in. sections are given in Fig. 25 to 30.

Discussion of Y-Block Data

Since the principal objectives of the work were (1) to obtain sound test bars and (2) to provide wider coverage of cooling rates, the data will be discussed under these two divisions. A short discussion of transverse testing will also be given.

Comparative Soundness of Arbitration Bars and Y-Blocks—In general, the frons at higher levels of tensile strength exhibit greater shrinkage. For example, in the arbitration bars, the ductile irons showed the worst cases of shrinkage with diminished amounts in the Class 50 and Class 30 irons. In the Y-blocks, no shrinkage was encountered in any case in the test section of the casting, that is, in the section beneath the enlarged head section. Examples of etched sections are shown in Fig. 14 and 15.

The mechanical properties are in general agreement with the visual inspection. In the case of ductile cast iron, the Y-blocks exhibit much better elongation and tensile strength than sections of comparable cooling rate in the arbitration bars. In the ferritic ductile irons the 0.875 and 1.2-in. bars are 5 to 15 per cent lower in elongation than the Y-blocks. The specimen from the 2-in. bar was taken from the bottom end with its centerline displaced ½ in. from the center of the bar and, therefore, shows better properties. In the pearlitic ductile irons the tensile strength and elongation are both superior in the Y-blocks in general. In the irons with flake graphite, the difference between test bars is slight except of the Class

50 iron.

The unsoundness at the center of the arbitration bar is shown better by the undersize specimens cut from this region, Table 3. These specimens are inferior in all cases to the larger specimens, the differences being most pronounced in the ductile irons.

At this point it is interesting to examine the basic reasons for the soundness of the Y-blocks and the shrinkage in the arbitration bars. It is axiomatic in the foundry that to provide sound castings the solidification must proceed toward the riser, in other words, that proper directional solidification must be obtained. The mere existence of a thermal gradient is not enough, however, since all the test bars solidified toward the riser (Fig. 20 and 21). To combat effects such as the irregular growth of large dendrites which tend to wall off the lower portions of the bar from the source of liquid metal, the *proper* thermal gradient is needed to produce controlled directional solidification and this will vary with the particular alloy.

To obtain a quantitative measure of the difference in the thermal gradient of the Y-blocks and arbitration bars, the cooling curves at different locations were obtained as shown in Fig. 20 and 21.

The thermal gradient was calculated as the difference in temperature per unit length of casting at a given temperature. For example, just above the final solidification (eutectic) of the 1-in. Y-block, Fig. 20, the temperature difference between the lowest and middle stations is 50 F. Since the distance is $1\%_{16}$ in., the thermal gradient is 42 F per in.

The temperature chosen for determination of the thermal gradient depends, of course, on the particular phenomenon being studied. For investigating feeding, since the most difficult and critical time is the final

²See Fig. 11 for Y-blocks, Fig. 12 for arbitration bars.

³ Elongation figures obtained from SR-4 strain gages. All other elongation figures obtained by dividers.

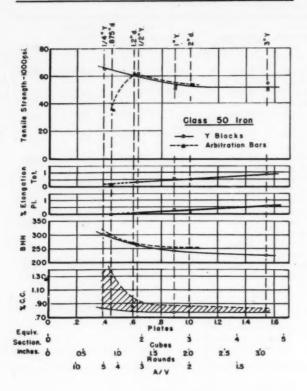
^{*1.250} Th. specimens made 1.150 in, diam in ductile iron so breaking strength would be under 100,000 lb.

⁸ C location is same as A location on the opposite side of the casting.

⁶ Not obtained.

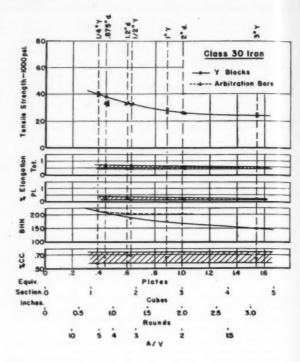
Mottled-See Fig. 15.

⁸ Broke in threads.



T.C. Mn P S Si Mo 2.96 1.05 0.114 0.072 1.63 0.67

Fig. 18-Mechanical properties of Class 50 iron.



T.C. Mn P S Si 3.40 0.71 0.423 0.152 2.38

· Fig. 19-Mechanical properties of Class 30 iron.

TABLE 3-MECHANICAL PROPERTIES OF ARBITRATION BARS OBTAINED BY ASTM AND 0.505 SPECIMENS

| | | | ASTM Se Specia | | 505-in Sh. Specia | |
|-----------|------|-----------|-----------------------------|---------------------------------------|-----------------------------|---------------------------------------|
| Iron | С | asting | Tensile Strength, psi | Total Elon- gation, per cent | Tensile Strength, psi | Total Elon- gation, per cent |
| Pearlitic | 1.2 | -in. Bar | 100,420 | 2.7 | 91,500 | 1.0 |
| Ductile | 1.2 | -in. Bar | 87,200 | 2.7 | 92,750 | 0.5 |
| | 2 | -in. Bar | 103,100 ¹ | 1.5 | 86,000 | 1.5 |
| | | | | | 99,500 | 2.0 |
| Ferritic | 0.87 | 5-in. Bar | 54,500 | 4.5 | 34,000° | 0.0 |
| Ductile | 1.2 | -in. Bar | 68,200 | 15.0 | 64,750 | 9.0 |
| | 2 | -in. Bar | 70,700 | 22.0 | 63,000 | 7.5 |
| Class 50 | 1.2 | -in. Bar | 62,400 | | 60,000 | |
| | 2 | -in. Bar | 54,100 | | 47,500 | |
| | 2 | -in. Bar | 54,500 | | 50,250 | |
| Class 30 | 1.2 | -in. Bar | 33,925 | 0.73 | 28,250 | |
| | 1.2 | -in. Bar | 33,450 | | 29,500 | 0.56 |
| | 2 | -in. Bar | 26,620 | 0.58 | 21,500 | |
| | 2 | -in. Bar | 26,200 | | 23,000 | 0.54 |

¹ 0.797 Th. specimen machined from sound metal at stub end of bar.

TABLE 4—TEMPERATURE GRADIENTS OBTAINED IN Y-BLOCKS AND ARBITRATION BARS

| Casting | 2110 F Thermal Gradient Between Thermocouple Stations A and B °F/In. | 1800 F Thermal Gradient Between Thermocouple Stations A and B °F/In. |
|----------------|---|---|
| 1/4-in. Y | 61 | 266 |
| 1/2-in. Y | 59 | 235 |
| 1-in. Y | 42 | 109 |
| 3-in. Y | 38 | 42 |
| 0.875-in. Dian | Arb. Bar 9 | 17 |
| 1.2-in. Dian | Arb. Bar 3 | 13 |
| 2.0-in. Dian | Arb. Bar 3 | 10 |

TABLE 5—Y-BLOCK SERIES SHOWING MECHANICAL PROPERTIES OF IRREGULAR DUCTILE IRON

| | | | | | Grap | hite T | ype |
|-------------------------|---|---------------------------------|---------------------------------|-----|--------------|---------------------|------------|
| Y-bloo Sectio in. | | Tensile Strength, nen psi | Per Cent Total Elongation | BHN | % Nodular | % Quasi Flake | % Flake |
| 1/2 | A | 120,000 | 4.0 | 269 | 90 | 10 | |
| | В | 108,400 | 3.0 | 255 | 90 | 10 | |
| 1 | A | 95,000 | 3.0 | 255 | 70 | 20 | 10 |
| | В | 69,500 | 1.5 | 255 | 50 | 30 | 20 |
| 3 | A | 73,250 | 3.0 | 207 | 50 | | 50 |
| | В | 38,500 | 0.5 | 196 | 30 | | 70 |

^a Undersize bar in this case was a 0.252 Sh. specimen.

BARS

e

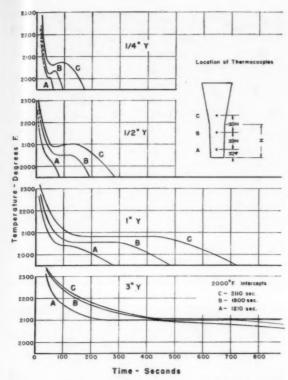


Fig. 20—Cooling curves of Y-blocks obtained with Pt-10% Pt-Rh thermocouple encased in silica tubing on pearlitic ductile iron with a Brown six point recorder.

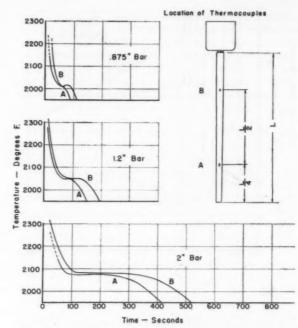
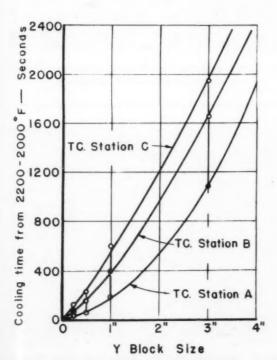


Fig. 21—Cooling curves of arbitration bars obtained with Pt-10% Pt-Rh thermocouples encased in silica tubing on pearlitic ductile iron with a Brown six point recorder.

solidification, the thermal gradient just above this point is of interest. On the other hand, in observing the development of internal stresses, the thermal gradients in the range 1400 to 900 F are most important.



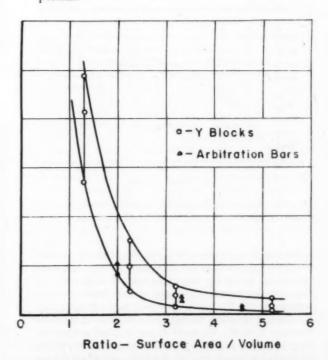


Fig. 22-Relationship between Y-block size, area/volume ratio and cooling time from 2200 to 2000 F.

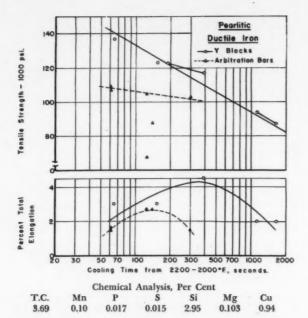


Fig. 23—Relationship between cooling time (2200 to 2000 F) and tensile strength of pearlitic ductile iron.

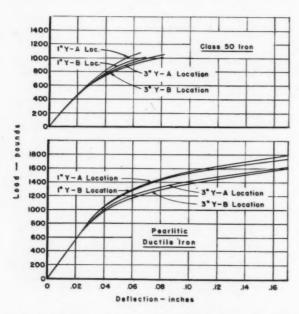


Fig. 24—Transverse properties of pearlitic ductile and Class 50 iron specimens from Y-blocks.

For this investigation, however, the gradient existing just above the start of the final solidification has been calculated by using the temperature difference between the lower and upper gage points (thermocouple stations A and B), when the lower point reaches 2110 F. Table 4 indicates the marked difference in gradient between Y-blocks (38 to 61 F per in.) and arbitration bars (3 to 9 F per in.). This difference is believed to be the prime factor causing the difference in shrinkage.

This thermal gradient does not persist in certain

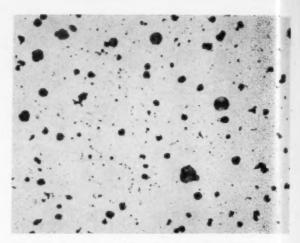


Fig. 25-Structure of \(\frac{1}{2}\)-in. Y-block of pearlitic ductile iron. Mignification 100X. Unetched.

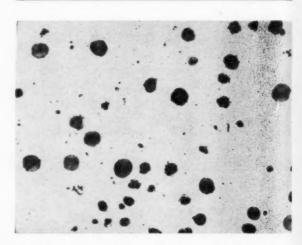


Fig. 26-Structure of 3-in. Y-block of pearlitic ductile iron. Mignification 100X. Unetched.



Fig. 27-Structure of ½-in. Y-block of Class 50 iron.

Magnification 100X. Unetched.

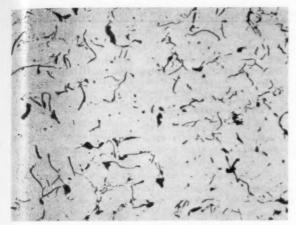


Fig. 28—Structure of 3-in. Y-block of Class 50 iron. Magnification 100X. Unetched.



Fig. 29—Structure of 1/2-in. Y-block of Class 30 iron. Magnification 100X. Unetched.



Fig. 30-Structure of 3-in. Y-block of Class 30 iron. Magnification 100X. Unetched.

cases during the eutectic solidification. For example, in the 3-in. Y-block, both thermocouple stations are close to 2100 F at 440 sec after pouring. However, the lower station will be solid before the upper because of the earlier entry in the eutectic range. In other words, a certain fixed amount of heat will be liberated at both stations due to eutectic solidification, but the direction of solidification will be governed by the prior thermal gradient.

To corroborate this point of view, the temperature gradients after solidification have been calculated (Table 4), using 1800 F as a basing point and are comparable to the 2100 F effects. It should also be mentioned at this point that the gradients calculated at 2110 F are not entirely free from the effects of heat of solidification since austenite is precipitating in this range. The amount of this precipitation is proportional to the drop in temperature rather than being isothermal and occurs in approximately equal quantities at both stations and, therefore, the effect cancels out. Below the eutectic, carbide precipitation provides an analogous, but much slighter effect on the thermal gradients in the solid.

To summarize the data of this section, the Y-blocks exhibit sound test bar locations, compared with unsound metal in the arbitration bars. The differences are pronounced in the ductile irons as reflected by the mechanical tests, but can only be shown in the irons with flake graphite by using 0.505 in. diam specimens. The controlled directional solidification of the Y-blocks is caused by the greater thermal gradient (at 2110 F) of 38 F per in. to 61 F per in. in the Y-blocks, compared with 3 F per in. to 9 F per in. in the arbitration bars.

Range of Cooling Rates—The second objective: "to provide a greater range of cooling rates than now obtained in the standard bars" is equally important as producing sound specimens. Section size effects can be far more pronounced in cast iron than in steel because of the possibility of massive carbides in light sections and of free ferrite and coarse graphite in heavy sections. In the case of improperly treated ductile irons nodular graphite may be encountered in light sections, but flake type may predominate in heavy sections of the same heat (Table 5).

Although three sizes of arbitration bar are available, the 1.2-in. diam has tended to become a single standard. Also, the 1.25-in. diam tensile specimen has discouraged the use of the 2-in. bar because it is difficult to break either because of machine capacity or grip strength. From the foregoing section, it is apparent that the chief reason for this large diameter tensile specimen is to minimize the effect of the centerline shrinkage.

Even if all three types of arbitration bars were in common use, the range of available cooling rates is much narrower than covered by the Y-blocks. In order to make comparisons between bars and blocks it is necessary to find a common scale. An established method is the area to volume ratio. The surface area of the casting and the volume are calculated and the result is related to the cooling rate. Simple castings with the same area/volume (A/V) ratio have approximately the same cooling rate. For example, a 1-in.

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TABLE 6-TRANSVERSE PROPERTIES OF DUCTILE, CLASS 50, AND CLASS 30 IRON

| Iron | Casting | Span, In. | Diam, In. | Load, Lb | Deflection, | Modulus of Rupture, psi | Secant Modulus, 10 ⁶ psi | Tangent Modulus, 10° psi | Tangent Modulus From SR-4 Gages on Tensile Specimens 10° psi |
|-------------------|--|----------------|-------------------|-----------------------------|-------------------------|---------------------------------|---|--------------------------------|--|
| Pearlitic Ductile | 0.875-in. Arb. Bar 0.875-in. Arb. Bar | 12 12 | 0.875 0.875 | 4000 3700 | 0.509 0. 39 9 | 182,000 168,500 | 9.8 11.6 | | - |
| | 1.2-in. Arb. Bar 1.2-in. Arb. Bar 1.2-in. Arb. Bar Poured Hori- zontally | 18 18 18 | 1.2 1.2 1.2 | 5720 5760 7100+ | 0.566 0.551 0.75+ | 152,200 153,500 Could not | 12.1 12.5 be broken. | | |
| | 2.0-in. Arb. Bar 2.0-in. Arb. Bar | 24 24 | 2.0 | | ot be broken | | _ | | |
| | 1-in. Y 1-in. Y | 5 5 | 0.500 0.500 | 1900 ¹ | 0.2079 0.2148 | _ | _ | 24.2 23.6 | 25.5 |
| | 3-in. Y 3-in. Y | 5 5 | 0.500 0.500 | $\frac{1700^{1}}{1700^{2}}$ | 0.1966 0.2122 | _ | _ | 23.9 23.9 | 25.5 |
| Class 50 | 0.875-in. Arb. Bar 0.875-in. Arb. Bar | 12 12 | 0.875 0.875 | 1540 1560 | 0.109 0.099 | 70,200 71,100 | 17.7 19.7 | | |
| | 1.2-in. Arb. Bar | 18 | 1.2 | 2780 | 0.223 | 74,100 | 14.9 | | |
| | 2.0-in. Arb. Bar 2.0-in. Arb. Bar | 24 24 | 2.0 2.0 | 11,000 10,500 | 0.317 0.270 | 84,100 80,300 | 12.7 14.3 | | |
| | 1-in. Y | 5 5 | 0.500 0.500 | 1080 1010 | 0.0649 0.0684 | 110,200 103,000 | 14.2 12.5 | 20.2 20.2 | 20.2 |
| | 3-in. Y | 5 5 | 0.500 0.500 | 1060 1010 | 0.0813 0.0804 | 108,100 103,000 | 11.1 10.7 | 19.8 19.7 | 21.7 |
| class 30 | 0.875-in. Arb. Bar 0.875-in. Arb. Bar | 12 12 | 0.875 0.875 | 1360 1220 | 0.150 0.126 | 62,000 55,600 | 11.3 12.1 | | |
| | 1.2-in. Arb. Bar 1.2-in. Arb. Bar | 18 18 | 1.2 1.2 | 2020 1840 | 0.210 0.190 | 53,900 49,000 | 11.5 11.6 | | |
| | 2.0-in. Arb. Bar 2.0-in. Arb. Bar | 24 24 | 2.0 2.0 | 5980 6060 | 0.244 0.253 | 45,600 46,400 | 9.0 8.8 | | |
| | 1-in. Y | 5 5 | 0.500 0.500 | 640 570 | 0.0633 0.0640 | 65,300 58,200 | 8.6 7.6 | | |
| | 3-in. Y | 5 | 0.500 0.500 | 570 520 | | 58,200 53,100 | _ | | |

plate, a 2-in. round bar and a 3-in. cube that have the same A/V ratio would also have the same cooling rate. The larger the A/V ratio, the faster the cooling rate.

As an index of cooling rate, the cooling time from 2200 to 2000 F has been obtained from the curves. The plot of cooling time 2200 F/2000 F against A/V ratio shows a consistent relationship between arbitration bars and Y-blocks, Fig. 22.

The range of cooling times of the test sections of the Y-blocks (2200–2000 F) is much greater than obtained with the arbitration bars, 36 to 1656 sec and 57 to 422 sec respectively.

This difference in cooling time has an important effect upon mechanical properties as shown in Table 2 and Fig. 16, 17, 18, 19. In Fig. 23 the cooling time of each test bar location rather than the average time of the block is plotted against the tensile strength. The graph shows that different properties are obtained not only in the various blocks, but also in specimens of different cooling rate cut from the same block. The

cooling rate of a casting should be matched, therefore, with a given gage location and not merely a certain block when the best accuracy is desired.

The pearlitic ductile iron and the Class 50 iron indicate the use of the ¼-in. block as a chill test for light castings. The lower and faster cooling portion of this block showed considerable chill while the upper region did not (Fig. 15).

The 1/4-in. Y-block of the Class 30 iron shows markedly improved properties over the heavier sections; 40,000 psi vs 28,000 psi in the 1-in. Y-block. The general use of the 1.2-in. bar tends to conceal the good properties of the Class 30 iron in the lighter sections for which it is particularly suited and at the same time to mask the chilling tendency of the higher strength irons.

Transverse Testing

Although the primary purpose of the investigation was to provide better tensile data over a wider variety us

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of sections, an improved transverse bar and testing procedure have been developed. See Appendix.

Comparison of the data of Table 6 and Fig. 24 indicates that excellent reproducibility of tangent modulus of elasticity, and of load deflection values is obtained with the sound machined bars cut from

The spread between specimens cut from the same block is a real difference due to cooling rate, not a lack of reproducibility. The specimens decline in secant modulus and deflection approximately in the order of cooling rate.

In the present standard transverse bars, the spread in deflection is caused by the rough surface of the bar, shrinkage and the method of observing deflection.

The machined transverse bar offers a convenient method of observing both tangent modulus of elasticity and, in the irons with flake graphite, of estimating ductility. The modulus values from the transverse test are in good agreement with the tensile test and were calculated by the standard beam formula applied to the straight line portion of the graph:

$$E = \frac{4}{3} \frac{1^{3}}{\pi^{d^{4}}} \frac{P}{y} = 849. \frac{P}{y} \text{ for the test bar used.}$$
Where $E = \text{Modulus of elasticity, psi}$

$$1 = \text{Distance between supports, in.}$$

$$P = \text{Load, lb}$$

$$y = \text{Deflection, in.}$$

$$d = \text{Diameter of test bar, in.}$$

Conclusions

1. No shrinkage was encountered in the test sections of the Y-blocks while some shrinkage was encountered in all sizes of arbitration bars in castings made of two types of ductile cast iron, Class 50 and Class 30 cast irons. These differences in soundness are caused by the low thermal gradient (3 to 9 F/in.) in the arbitration bars, compared with 38 to 61 F per in. in the Y-blocks.

2. Mechanical properties of the arbitration bars were lower than of the Y-blocks of comparable cooling rate, in the case of the ductile cast irons, corresponding to the greatest shrinkage in the arbitration bars. Properties in the irons with flake graphite were approximately the same with standard size tensile specimens, but lower with 0.505-in. diam specimens.

3. The series of 1/4-in. to 3-in. Y blocks provides a greater range of cooling rates than the present arbitration bars. Cooling time through the solidification 2200 to 2000 F range is 36 to 1656 sec, compared with 57 to 422 sec for the arbitration bars.

4. The increased cooling range of the Y-blocks shows a wider range of mechanical properties. The 1/4-in. Y-block indicates the chilling tendencies of the higher strength irons and the good mechanical properties of the so-called lower strength irons in light sections.

5. The transverse bars machined from Y-blocks provide more accurate and more reproducible data than the sand cast bars because of revision of the test method and the better specimen condition.

6. In view of the above results, the substitution of the Y-block series for the present arbitration bar series is recommended for consideration by the appropriate A.F.S. and ASTM Committees.

Acknowledgment

The authors are greatly indebted to the entire staff of the Metallurgical Department of the American Brake Shoe Co., especially to Mr. R. H. Schaefer, Director of Research, for many helpful criticisms and suggestions. They also appreciate greatly the metallographic work of Mr. John Gleassey.

The criticisms and suggestions of the A.F.S. Test Bar Committee members have been invaluable and are gratefully acknowledged.

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2. H. F. Taylor, Report of the G.I.F.S. Technical Committee,

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3. J. H. Schaum, "Gray Iron Transverse Test Bars," The Foundry, Dec. 1948, pp. 68-71, 216 and Jan. 1949, pp. 82-85, 222. 4. J. T. MacKenzie and C. K. Donoho, "A Study of the Effect of Span on the Transverse Test Results for Cast Iron." Proceedings, ASTM, vol. 37, part II, pp. 71-87 (1937).

APPENDIX

The MacKenzie apparatus for accurately measuring transverse deflections consisted of an "Ames dial reading to 0.0001 in. attached to a yoke. The yoke hung from two 1/16-in. round pins through the neutral axis of the bar exactly above the support points. The tip of the dial rested on the bottom of the bar at the center of the span and directly beneath the load point. By this method, the crushing of the bar at the support points, the compression of the supports, etc. did not appear as deflection."

DISCUSSION

Chairman: A. P. GAGNEBIN, International Nickel Co., Inc., Bayonne, N. J.

Co-Chairman: W. B. McFerrin, Haynes Stellite Div., Union Carbide and Carbon Corp., Kokomo, Ind.

T. E. EAGAN (Written Discussion):1 The authors are to be congratulated on contributing an excellent paper. They and the Test Bar Design Committee have spent many long and laborious hours in developing the information presented.

The standard arbitration bar, in spite of the fact that it is subject to shrinkage and does not always represent the metal in the casting, has certain definite advantages which we cannot easily overlook. Some of these are:

1. Easy to mold and cast.

2. Easy to machine into tensile bars.

3. Easy to use for determination of transverse properties. Some of the objections to the Y-bar, in spite of the fact that it will give solid metal are:

1. The test piece must be cut off the bar.

2. That it will cost more to machine a test bar from the cut off piece.

In view of the increased cost of using the Y-bar, it will be difficult to establish it as an absolute substitute for the arbitration bar. However, it could certainly be established as the correct type of bar for the higher tensile gray irons where the arbitration bar does not give adequate representation of the metal.

In the case of the nodular or ductile cast irons, the 1-in, keel block used for cast steel has been tentatively adopted by the manufacturers. The shrinkage experienced in the arbitration bars precludes its use. The use of the Y-bar for this type of metal seems logical.

W. K. Bock: The authors stated that the difference in properties that they obtained from a bar out of the coupon from the

² Chief Metallurgist, The Copper-Bessemer Corp., Grove City, Pa.

² Research Engineer, National Malleable & Steel Casting Co., Cleve-

bottom and the top is due to differences in cooling rates. The fact that they have found an assignable cause is a comment on the thoroughness of this work. But the trouble still exists and it appears like the complaint we used to have on the old trefoil block in steel foundries which has fallen into disrepute. I think you may find with more extended use of this wide bar that there will be those same complaints-these borderline heats with the bottom bar usually passing a grade and the upper bar somewhat doubtful. Those situations are always annoying.

I wonder, in the light of the steel founders' experience and what the authors have shown here, whether they have considered a design where two or four coupons are all located similarly

with respect to the hot metal reservoir or feeder.

Then there is this question of having a test bar designed which can be scaled to match the section size being cast. Sections of 11/4 in. are ordinarily poured regardless of the section sizes that are cast at the same time. The authors' proposal should give much more useful data without the bother of sectioning a casting

H. W. STUART:3 This test bar paper is nothing radically different. One member of this Committee acts as liaison to the ASTM Committee A-3 which will study the work herein presented by the authors. Development of any new specification

will take considerable time.

I think we should be very tolerant of this new approach to the test bar problem because we are using in this work, tools that we did not have when Dr. Moldenke and others were working on correlation of test bars and castings. If we as producers do not sponsor the use of these tools in test bar work, we are going to have much more active consumer interest in specifications than it has been.

Little reference is made in the present ASTM specification to ductility. As the demand for specified values of ductility appear whether or not test bars are machined and how they are

cast becomes very important.

C. O. Burgess:4 Should not the use of a breaker core between the feeder head and the rectangular portion of the test bar make this bar a more economical product from the foundryman's point of view. This would eliminate a sawing operation particularly in the case of nodular irons.

C. K. Donoho of the American Cast Iron Pipe Co., Birmingham, Ala., recently made some tests at our request and reported considerable success with these breaker cores and using the

standard keel block test piece.

MR. KRAFT: Mr. Eagan listed three advantages of the arbitration bars. One of these was ease in molding. We agree that it is easy to mold, but the Y-block is also quite easy to mold.

Concerning the machining of tensile bars in these two types of castings, it is true that you do have a round segment to begin with in the arbitration bar, requiring little cut-off, and in case of Y-blocks tensile blanks must be cut from the end of the block requiring either a saw or a cutoff machine. We cut them off with an abrasive wheel cutoff machine in 1 or 2 min. It can be done with a hacksaw in 5 min. A compensating advantage of the Y-blocks compared to the arbitration bars is that threaded tensile specimens are not needed. Of course, making tensile

² Director of Quality Control, U. S. Pipe and Foundry Co., Burling-

specimens from the 1/4-in. block requires a milling machine and is more expensive.

Transverse testing of arbitration bars is cheaper but error as high as 30 per cent in modulus and load were observed. Transverse bars must be machined from Y-blocks which cost some-

thing but accurate results are obtained.

Mr. Eagan also commented on weaker irons. In these irons the arbitration bars give comparable data since shrinkage is not pronounced. However, we feel that there should be one standard for all types of cast irons to prevent misunderstandings. It comes back to the fact that there is no shrinkage in Y-blocks in any section size, and they have a wider span of cooling rates. The span of cooling rates in the arbitration bars (the cooling time from 2200 to 2000 F) is something like 400 sec, and in the Y-blocks it is about 1600 sec. In addition, the lighter Y-blocks reveal the advantages of the so-called weaker irons, and show the chilling tendencies of the stronger irons. The arbitration bars, of which the 1.2-in. is tending to become standard, masks both of these effects.

We agree with Mr. Eagan that for the testing of ductile iron the 1-in, keel block can be used since it is sound, but the data should not be compared with the 1-in. Y-block since the large head of the keel block would slow down the cooling rate of the test lugs. It would require a thermocouple survey to determine just where the test coupon of the 1-in. keel falls in the plate section series, although the A/V ratio of the block would give a

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rough indication.

Regarding the correlation of test bars with casting properties, we sectioned a number of different types of castings and found the test bar properties from the Y-blocks correlate quite well with the actual casting properties. It is true that the only way you can determine precisely which coupon from which Y-block to use is to insert thermocouples into the castings. But if you are on a flat portion of the curve you can span the casting by cutting selected tensile bars from two sizes of Y-blocks. There is certainly more flexibility for this type of testing using the Y-blocks rather than the arbitration bars.

Mr. Eagan's comments about the pearlitic interval in heavy sections are well founded. We believe Y-blocks will have the same interval as castings of equivalent A/V ratio. To study this effect in heavier sections we are designating a larger Y-block with an equivalent plate section of over 3 in. (The 3-in. Y had an equivalent plate section of about 11/2 in.).

Mr. Bock commented that he would want two test bars as a check, both of them located at the same level. In the keel blocks both coupons have the same cooling rate and can be used as a check. We can confirm our data by cutting the same coupon from duplicate castings.

We agree with Mr. Stuart's comments that ductility is going to be more important now, and we feel the Y-blocks, being sound, are a more accurate indication of this than are the arbi-

tration bars.

It has been suggested than a break-off core be tried. We did not do that on this work but I think it is a good suggestion, and something that should be investigated. We had two test bar locations in these Y-blocks, the lower and the upper. You could either insert two break-off cores or just shorten the casting by having one blank and one core. Of course, if you shorten the casting, you are going to alter the cooling rate.

ton, N. J. 4 Technical Director, Gray Iron Founders' Society, Cleveland.

FATIGUE DATA SUMMARY

Report No. 1

By M. E. Annich*

ABSTRACT

This report is an interpretation of data on fatigue factor values used by the Foundry Industry in its production operations. The A.F.S. Timestudy and Methods Committee circulated a questionnaire in the Foundry Industry and compiled the required data herein presented.

FATIGUE DATA RECEIVED by the Timestudy and Methods Committee of the American Foundrymen's Society from various members in response to a questionnaire sent out during the latter part of 1949 was very gratifying.

It is believed that progress will be made on this phase of the work from a thorough analysis of these data, a presentation of several reports on it, and a complete discussion of the whole problem.

Analyzing all the data received will take a long time. In order therefore, to present something to the members for discussion at the 1950 Convention, only one method of application of fatigue allowances will be covered by this report.

Since 27 of the 54 reporting plants apply fatigue in whole or in part on an overall basis, the first report will deal with this method. It will endeavor to cover the following points.

 To summarize all the data in the above mentioned reports by operation, and by weight or flask size in those operations, and show the averages of the values reported.

To set up from the above reported values a table of "Ideal" average values on various operations.

To derive formulas for application of overall fatigue allowances to various operations where the reported data are sufficient, based on weight or flask size.

It is to be emphasized that the values developed in this report must not be used without considered judgment as to their source and derivation, and with due consideration to the various factors on any operation on which they are to be applied, that might make that operation different from the average.

It should also be remembered that the fatigue values developed are to be applied over all the elements of an operation. A subsequent report or reports will deal with methods of applying fatigue allowances on an elemental basis, and with special methods in use by other plants which the Committee received in response to the questionnaire.

Table 1 is a summary, by operation, of the reports of the 27 plants, showing the average fatigue allowed,

TABLE 1

| | Light (0 - 30 lb) | Medium (30 - 100) (1 | Large 00 lb min) |
|-----------------------------|-------------------|-------------------------|---------------------|
| Chipping, Air | (9) - 9.8 | (10) - 10.8 | (9) - 14.1 |
| Chipping, Hand | (1)-10.0 | (1) - 15.0 | - |
| Core Finishing | _ | (2) - 10.0 | _ |
| Core Knockout | | (1) - 5.0 | (3) - 13.3 |
| Core Paste & Assembly | | (2) - 5.0 | (2) - 5.0 |
| Coremaking, Bench | (15) - 5.4 | (5) - 9.6 | (1) - 13.5 |
| Coremaking, Blower | (8) - 6.1 | (4) - 10.1 | (2) - 14.3 |
| Coremaking, Floor-Hand | _ | (2) - 5.0 | (2) - 5.0 |
| Coremaking, Machine | - | (3) - 7.5 | (2) - 8.8 |
| Coremaking, Rollover Machin | ne (2) - 6.3 | (4) - 12.5 | (4) - 14.0 |
| Cut-off Machine | (2) - 5.0 | (2) - 11.0 | _ |
| Grind, Hand Grinder | (3) - 9.0 | (4)-10.6 | (6) - 12.0 |
| Grind, Stand | (5) - 7.8 | (3) - 8.3 | _ |
| Grind, Swing | _ | (6) - 9.1 | (3) - 11.3 |
| Hydroblast | (1) - 5.0 | _ | |
| Inspection | (4) - 6.1 | (4) - 8.1 | (2) - 12.5 |
| Molding, Floor | (1) - 5.0 | (1) - 15.0 | (3) - 13.3 |
| Molding, Bench-Hand | | _ | _ |
| Molding, Finishing | _ | (1) - 12.5 | _ |
| Molding, Large Machine | - | _ | (4) - 10.0 |
| Molding, Medium Machine | _ | (9) - 9.3 | (8) - 11.0 |
| Molding, Slinger | _ | | (5) - 9.9 |
| Molding, Squeezer | (14) - 8.0 | (14) - 9.2 | (6) - 15.9 |
| Press, Cut-off | (3) - 6.3 | (1)-15.0 | _ |
| Press. Power | (3) - 10.0 | (2) - 13.8 | _ |
| Shear | (1) - 7.5 | (1) - 13.5 | (1) - 20.0 |
| Rotoblast | _ | (2) - 12.5 | (2) - 12.5 |
| Sandblast | _ | (3) - 16.7 | (3) - 16.7 |
| Shakeout, Large Mold | _ | _ | (3) - 9.3 |
| Shakeout, Squeezer Mold | _ | _ | _ |
| Weight Changer | | _ | (1) - 10.0 |
| Welding | _ | (1) - 9.0 | (1) - 12.0 |

^{*} Supt. of Standards, American Brake Shoe Co., Mahwah, N. J. This is a report of the Timestudy and Methods Committee of the American Foundrymen's Society. Personnel of this Committee are as follows: E. G. Tetzlaff, Chairman, M. E. Annich, Roy Diffenbaugh, J. J. Farkas, Wally E. George, H. Reitinger, M. T. Sell, Dean Van Order, Jeff Alan Westover and H. R. Williams.

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ng he and (in parentheses) the number of reporting plants averaged. Under the columns headed—Light, Medium and Heavy the following ranges have been used, as representing most correctly the values that were reported. Where no values were reported, or where the operations were not designated as Light, Medium or Heavy, they were included in the most logical category or disregarded altogether, at the disgression of the analyst.

On all operations on a total weight basis:

Light jobs range from 0 to 30 lb.

Medium jobs range from 30 to 100 lb.

Heavy jobs range from 100 lb up. On Squeezer molding, on a total flask volume basis:

Light jobs range up to 1200 cu in. Medium jobs range from 1200 to 2500 cu in.

Heavy jobs range from 2500 cu in. up.

On Medium Machine molding, on a total flask volume basis:

Light jobs range up to 5000 cu in.

Medium jobs range from 5000 to 10000 cu in. Heavy jobs range from 10000 cu in. up.

On floor molding, no specifications as to flask size were reported. The average fatigue values consist of those which were simply designated, Small Floor, Medium Floor and Large Floor.

In an endeavor to find a set of "Ideal" averages for the various operations, and to fill in the blank spaces in Table 1, as well as to correct those values where the average is not made up of enough occurrences to be representative, one assumption had to be made.

The assumption is that the differential in fatigue

between Light and Medium and between Medium and Heavy in Table 1 should be the same in all operations. An attempt was then made to evaluate these differentials. This was done by taking a simple average of the differentials for each operation where the values in adjacent categories for a given operation were an average of three or more occurrences.

This procedure resulted in the following differentials which were used in developing Table 2. The average differential between the Light and Medium categories of all operations is 1.8 per cent. The average differential between the Medium and Heavy categories of all operations is 2.4 per cent.

Table 2 might be considered as the "Ideal" average of the 27 reporting plants. It can be used to good advantage as a guide to the variation in fatigue allowance from one operation to another, and from one category to another in the same operation.

Rearranging the "Ideal" table from an alphabetical order to one in ascending order of the fatigue allowance we get Table 3, which will further facilitate general comparisons of one operation with another so that the fatigue values can the more readily be confirmed or disproved.

It should be stated at this point, that the averages used in Table 1, from which Tables 2 and 3 were developed, are made up only of values which each plant reported as being satisfactory in practice. Values considered too high or too low by the reporting plants were not used.

The accompanying graphs are presented to show by comparison with actual values used by the report-

TABLE 2

| Operation | Light (0-30 lb) | Medium (30–100 lb) | Large (100 lb min) |
|------------------------------|-----------------|-----------------------|-----------------------|
| Chipping, Air | 10.0 | 11.8 | 14.2 |
| Chipping, Hand | 10.0 | 11.8 | 14.2 |
| Core Finishing | 7.0 | 8.8 | 11.2 |
| Core Kneckout | 9.0 | 10.8 | 13.2 |
| Core Paste & Assembly | 5.0 | 6.8 | 7.2 |
| Coremaking, Bench | 5.0 | 6.8 | 9.2 |
| Coremaking, Blower | 6.0 | 7.8 | 10.2 |
| Coremaking, Floor-Hand | 5.0 | 6.8 | 9.2 |
| Coremaking, Machine | 5.0 | 6.8 | 9.2 |
| Coremaking, Rollover Machine | 9.0 | 11.8 | 14.2 |
| Cut-off Machine | 7.0 | 8.8 | 11.2 |
| Grind, Hand Grinder | 8.5 | 10.3 | 12.7 |
| Grind, Stand | 8.0 | 9.8 | 12.2 |
| Grind, Swing | 7.0 | 8.8 | 11.2 |
| Hydroblast | 10.0 | 11.8 | 14.2 |
| Inspection | 6.5 | 8.3 | 10.7 |
| Molding, Floor | 6.0 | 7.8 | 10.2 |
| Molding, Bench-Hand | 6.0 | 7.8 | 10.2 |
| Molding, Finishing | 7.0 | 8.8 | 11.2 |
| Molding, Large Machine | 6.0 | 7.8 | 10.2 |
| Molding, Medium Machine | 7.0 | 8.8 | 11.2 |
| Molding, Slinger | 6.0 | 7.8 | 10.2 |
| Molding, Squeezer | 8.0 | 9.8 | 12.2 |
| Press, Cut-off | 8.0 | 9.8 | 12.2 |
| Press, Power | 8.0 | 9.8 | 12.2 |
| Shear | 8.0 | 9.8 | 12.2 |
| Rotoblast | 7.0 | 8.8 | 11.2 |
| Sandblast | 10.0 | 11.8 | 14.2 |
| Shakeout, Large Mold | 8.0 | 9.8 | 12.2 |
| Shakeout, Squeezer Mold | 10.0 | 11.8 | 14.2 |
| Welding | 7.5 | 9.3 | 11.7 |
| Weight Changer | 9.0 | 10.8 | 13.2 |

TABLE 3

| 0 | Light | Medium | Large |
|------------------------------|-----------|-------------|-------------|
| Operation | (0-30 ID) | (30-100 lb) | (100 lb min |
| Core Paste & Assembly | 5.0 | 6.8 | 7.2 |
| Coremaking, Bench | 5.0 | 6.8 | 9.2 |
| Coremaking, Floor-Hand | 5.0 | 6.8 | 9.2 |
| Coremaking, Machine | 5.0 | 6.8 | 9.2 |
| Coremaking, Blower | 6.0 | 7.8 | 10.2 |
| Molding, Floor | 6.0 | 7.8 | 10.2 |
| Molding, Bench-Hand | 6.0 | 7.8 | 10.2 |
| Molding, Large Machine | 6.0 | 7.8 | 10.2 |
| Molding, Slinger | 6.0 | 7.8 | 10.2 |
| Inspection | 6.5 | 8.3 | 10.7 |
| Core Finishing | 7.0 | 8.8 | 11.2 |
| Cut-off Machine | 7.0 | 8.8 | 11.2 |
| Grind, Swing | 7.0 | 8.8 | 11.2 |
| Molding, Finishing | 7.0 | 8.8 | 11.2 |
| Molding, Medium Machine | 7.0 | 8.8 | 11.2 |
| Rotoblast | 7.0 | 8.8 | 11.2 |
| Welding | 7.5 | 9.3 | 11.7 |
| Grind, Stand | 8.0 | 9.8 | 12.2 |
| Molding, Squeezer | 8.0 | 9.8 | 12.2 |
| Press, Cut-off | 8.0 | 9.8 | 12.2 |
| Press, Power | 8.0 | 9.8 | 12.2 |
| Shear | 8.0 | 9.8 | 12.2 |
| Shakeout, Large Mold | 8.0 | 9.8 | 12.2 |
| Grind, Hand Grinder | 8.5 | 10.3 | 12.7 |
| Core Knockout | 9.0 | 10.8 | 13.2 |
| Coremaking, Rollover Machine | 9.0 | 11.8 | 14.2 |
| Weight Changer | 9.0 | 10.8 | 13.2 |
| Chipping, Air | 10.0 | 11.8 | 14.2 |
| Chipping, Hand | 10.0 | 11.8 | 14.2 |
| Hydroblast | 10.0 | 11.8 | 14.2 |
| Sandblast | 10.0 | 11.8 | 14.2 |
| Shakeout, Squeezer Mold | 10.0 | 11.8 | 14.2 |

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ing plants the "Ideal" fatigue allowance ranges developed in Tables 2 and 3. These graphs show the wide variations in allowances used from one plant to another, and point up the need for just such a survey of actual practice as the Committee is making.

The low values shown in the graph are no doubt due to the historical use of a straight 5 per cent fatigue allowance which in the past and still is to some extent, common practice. Three of the 54 reporting plants used a straight 5 per cent on all operations, one used a straight 6 per cent and one used 2 per cent.

Most plants have recognized the need for varying allowances on different operations, but in some cases the original 5 per cent may not be changed or may be adjusted to only a small extent, probably because the base standards are already considered too loose.

Fatigue allowances that are abnormally high compared to the average are probably a result of an adjustment which is called fatigue, to compensate for tight base standards.

The graphs were also developed in an attempt to evaluate by means of a formula, the fatigue allowance to apply on an overall basis for various flask sizes or weights handled.

VALUES PLOTTED IN FIG. 1-CHIPPING, AIR OR HAND*

| Plant Identification Number** | Mid-point of Weight Range Reported,*** lb | Per Cent Fatigu Allowed | |
|----------------------------------|--|----------------------------|--|
| 3 | . 50 | 5 | |
| 3 | 300 | 10 | |
| 9 | 1121/2 | 15 | |
| 9 | 350 | 15 | |
| 10 | 40 | 131/2 | |
| 13 | 60 | 5 | |
| 14 (Hand) | 15 | 10 | |
| 14 (Hand) | 65 | 15 | |
| 23 | 371/2 | 71/2 | |

* Since only two values for hand chipping were reported and these seemed to fall in line with values for air chipping they were included in this graph.

** Plant identification number is given merely to show how many different plants these values were obtained from.

*** Some reports merely stated the top weight range as being so many pounds—up. Where this occurred, 500 lb was used as the top of the range to determine mid-point of range. This seems logical, because, as seen from the graph, allowed fatigue at higher weights would be in smaller and smaller increments for equal increases in weight.

The formula, $F = 8.3 + (W)^{1/3.2}$, for the matching curve on Fig. 1 is given as a possible means of deter-

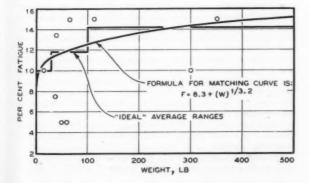


Fig. 1-Chipping, Air and Hand

mining the overall fatigue values for any operation on a weight basis in conjunction with the "Ideal" values given in Tables 2 and 3. This can be done by taking the differential between Air Chipping and whatever other operation is being considered, from the table, and by changing the constant value of 8.3 in the formula by this differential, the new formula is obtained.

For example:

From Table 3, the difference in the allowance between Air Chipping and Stand Grinding is 2.0 per cent. Subtracting this from the constant 8.3 per cent in the Air Chipping formula, the resulting formula for Stand Grinding would be

 $F = 6.3 + (W)^{1/3.2}$

As seen from Fig. 1, the value (W) $^{1/8.2}$ gives constantly diminishing values for F in the higher weight brackets, but it should be used with caution above 500 lb. Theoretically, there is little or no reason for higher fatigue allowances on weights requiring crane or hoist service for handling, so that above 1000 lb, the fatigue allowance should remain constant unless other elements than handling in the operation dictate otherwise.

VALUES PLOTTED IN FIG. 2-MOLDING, SQUEEZER

| Plant Identification Number | Mid-Point of Flask Volume Reported, Cu In. | Per Cent Fatigue Allowed |
|--------------------------------|---|-----------------------------|
| 4 | 1675 | 9 |
| 11 | 1470 | 10 |
| 11 | 3240 | 13 |
| 14 | 1675 | 10 |
| 14 | 2310 | 20 |
| 17 | 1675 | 10 |
| 17 | 2310 | 20 |
| 19 | 990 | 5 |
| 22 | 1150 | 8 |
| 22 | 1535 | 9 |
| 22 | 1055 | 6 |
| 22 | 2050 | 17 |
| 27 | 1970 | 5 |
| 27 | 3370 | 15 |

 Where no depth of cope and drag was reported, total depth was assumed to be 8 in.

Where lower limits of flask size were not reported minimum volume was assumed to be 1000 cu in. to determine mid-point of range.

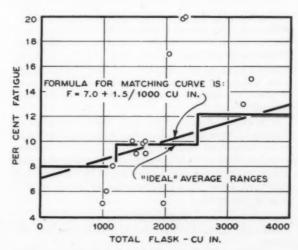


Fig. 2-Molding, Squeezer

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The formula derived in Fig. 2, F = 7.0 + 1.5/1000 cu in., being a straight line function of the volume is unlike that developed in Fig. 1, based on weight. Applying this formula to larger sizes of squeezer work would not result in loose allowances because the top limit on this type of work would rarely if ever, be over 4000 cu in. in which case the total overall fatigue allowance would be 10.0 per cent.

VALUES PLOTTED IN FIG. 3-MOLDING, MEDIUM MACHINE*

| Plant Identification Number | Midpoint of the Range in Volume, Cu In. | Per Cent Fatigue Allowed |
|--------------------------------|--|-----------------------------|
| 13 | 11200 | 5 |
| 13 | 18200 | 10 |
| 14 | 4200 | 10 |
| 14 | 7700 | 15 |
| 17 | 17575 | 7 |
| 22 | 6150 | 6 |
| 22 | 9600 | 9 |

 In plotting values on Fig. 3 top limit of total flask volume was assumed to be 20,000 cu in.

Where no flask depth was given total cope and drag depth was assumed to be 14 in.

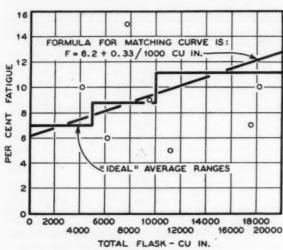


Fig. 3-Molding, Medium Machine

The formula developed in Fig. 3, F=6.2+0.33/1000 cu in. also shows that the fatigue allowance is a straight line function of the total flask volume. Such allowance is obviously lower than that for squeezer molding since in this operation so much more of the work is done by machine, and the handling by hoist or crane, the weights being too great to be handled manually.

It is hoped that the data and values presented in this report have enough foundation in actual practice to be considered worth using as a guide in making new installations, or in revising or extending old ones, so that through use, its validity might be confirmed or disproved. Only thus can progress be made in this field of endeavor.

DISCUSSION

Chairman: E. G. TETZLAFF, Pelton Steel Casting Co., Milwaukee.

Go-Chairman: M. T. SELL, Sterling Foundry Co., Wellington, Ohio.

MEMBER: Did these companies report on what allowances they used for certain elements and the method of arriving at these allowances?

MR. Annich: The questionnaire which the Committee circulated was worded so that we would get all that information and so that the answers would be consistent. We received a variety of answers. Some of them we could use and some we could not. We asked in the questionnaire for specific examples and also for data used, such as charts or graphs, in developing fatigue allowances. Such special methods will be presented in the next report.

G. R. CARPENTER: ¹ In this series is there any consideration as to the time cycle; how often the element occurs? Do you allow more when the element occurs more frequently?

MR. Annich: No, your frequency of occurrence is automatically taken care of by the timestudy time. Whenever a certain element occurs it gets the fatigue factor which is allowed.

MR. CARPENTER: Is that applied to the normal?

MR. ANNICH: Yes. However, I would like to point out that in applying allowances to the normal they should never be compounded (multiplied). For example, if you make a personal allowance of 5 per cent and let us say, other allowances amount to an additional 5 per cent plus a fatigue allowance of 30 per cent on a certain element, you would not multiply the 1.05 × 1.30 which would equal 143 per cent total allowance. Rather you would merely add the three allowances and make a total allowance of 140 per cent. You can get into trouble by compounding allowances and it is much simpler to add them as it should be done.

MEMBER: How would weight enter into the picture in the hand ramming operation with the hand traveling with or without a load?

Mr. Annich: The following chart shows per cent fatigue allowance we would use based on weight at these 100 per cent marks, for the knee to waist or waist to shoulder movements. These modification factors then, for the various classes of movement, combined with the weight chart will give you the entire picture.

PER CENT FATIGUE ALLOWED FOR KNEE TO WAIST OR WAIST TO SHOULDER MOVEMENT

| Weight Lifted or Force Exerted in lb | | Weight Lifted or Force Exerted in lb | Per Cent Fatigue Allowed, % |
|--|------|--|-----------------------------------|
| 0 | 4.0 | 60 | 34.5 |
| 5 | 5.5 | 70 | 40.5 |
| 10 | 7.5 | 80 | 46.5 |
| 15 | 9.5 | 90 | 52.0 |
| 20 | 12.0 | 100 | 57.5 |
| 30 | 17.0 | 110 | 61.5 |
| 40 | 22.5 | 120 | 65.5 |
| 50 | 28.5 | 130 | 69.0 |
| | | 140 | 71.0 |

MEMBER: What system do you use in your own plant?

MR. Annich: Unfortunately incentive in our company was started by industrial engineers before the idea of using a varying fatigue allowance came about. We use a straight 15 per cent; 5 per cent personal, 5 per cent relaxation and 5 per cent for supplemental operations. That was used right from the start. At one plant, when I first got into this standards work, we ran into trouble because on some of our heavier squeezer jobs, and we had some heavy ones, they just could not sell the standard. I worked up a table of fatigue allowances for this job (over and above the 5 per cent) based on flask weight and also flask size because we ran some flasks on these squeezer jobs 28 in. long where the man really had to stretch his arms to make the lift. We used the new fatigue allowances on this job and had no more trouble.

There are some cases where we have departed from the historical 5 per cent fatigue allowance where we found it was absolutely necessary.

My own personal experience has been that we have not been able to sell some jobs to the man in the shops because of the fact

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that we did not make more than a 5 per cent fatigue allowance on jobs that really required more. We never realized what it was. I think we know now that it is the fatigue allowance that was wrong.

MEMBER: During the survey did you prorate your fatigue

allowance during the rest period?

MR. ANNICH: We did in one report in particular where a certain job was running which required seven crews. They had eight crews but they were doing only seven operations and one crew just went around, relieving each crew in turn. It was called the standby crew. I think that was just straight day work. That was the way they were taking care of fatigue,

MEMBER: Whenever you decide that the regular allowances are not enough on a job, on what basis is it enlarged? We know that the man gets tired, but how tired does he get on a heavier

job than a lighter one. What basis do you use?

MR. Annich: That is exactly why we are having this survey. We want to find out what people are doing in the foundry industry so that we know about what allowances we should make. No answer to a questionnaire was used unless the plant reporting said it was satisfactory.

MEMBER: We use 15 per cent, simply because everyone else

uses this figure.

Mr. Annich: What we are trying to do is actually put it down in numbers and then through the experience of many people find out whether these numerical values are good or not. There are many medical people who have tried to measure

M. J. LEFLER: 2 You have a problem of selling the man on giving a timestudy on an element at full speed, if you are going to apply these factors to the element. Most operators slow down when they have an extra heavy or difficult job from long practice and if they are already slowing down to make their own loss for fatigue for an extra large mold, you are going to have to get them up to a normal pace, even if the mold is large, if you are going to apply these factors by weight or the distance to move.

Mr. Annich: I cannot quite agree with you because if you take a timestudy and the operator is operating at top speed on a certain element, you account for that do you not, by your pace rating and then you apply those factors on the normal pace.

MR. LEFLER: That may be true. You will have to use your judgment on what is the pace, then you will have to determine

these other factors.

Mr. Annich: Pace rating is another big problem in timestudy, but we are now discussing fatigue. I can understand your point of view. In modern industry today we are trying to get away from these high weights unless we believe that they will occur for a very short period of time. In other words, up to let us say lifting from 50 to 75 lb a man does not materially slow

Mr. Lefler: My point is that the man was already making his own estimate of how much he should slow down and he gets that back again before you apply these figures.

Mr. Annich: Yes, but you pace rate that slow-down out first and after you have your normal time you apply these allowances.

Mr. Lefler: Then you are starting from an indefinite point

to apply some definite factors, so far.

Mr. Annich: After all is said and done, timestudy is a tough problem. That is why we are working on it here and now MEMBER: Have you in your survey found that there is a difference in the fatigue allowances when a plant that is on measured day work only and one that is on the incentive plan?

Mr. Annich: I did not get any. There was one report that had measured day work but they did not give any facts and figures. I think it was an over-all fatigue that they allow; a straight 10 per cent, or some such figure. That is the only comparison that I have on measured day work. The rest were all on the incentive plan.

C. T. HASSELL: 8 You mentioned the element of fatigue allowance for lifting. If the element did not require lifting at all what would be the allowance? If the element work, for instance, in grinding, the time the man was pushing the casting toward the wheel or dropping it, what allowance would you use for

Mr. Annich: It is not only the weight lifted but the force exerted that must be taken into consideration. I did not mention that.

Mr. HASSELL: That would be same as from floor to floor or waist to waist.

Mr. Annich: That is right, but you would be exerting some kind of force.

MR. HASSELL: He would not be after he drops the casting but the motion would be considered as waist to waist motion.

Mr. Annich: Yes, it is in the same position.

I have never used this myself. I am just reporting what somebody else gave me. I think we can use our imaginations on this just a little bit. It is just a guide to using our imaginations.

Mr. Hassell: Did any of the plants consider fatigue in their job evaluation on a job evaluated base rate?

Mr. Annich: Several plants reported that they did not use any fatigue allowance whatever because they felt it was in their job evaluation.

MR. HASSELL: Do you not think it good practice to put it in? Mr. Annich: No, I do not think it takes care of the whole thing. In any pertinent evaluation that I have seen there is not enough allowance made on fatigue and moreover you cannot very well tie your fatigue down on a job evaluation classification. We have an entirely different operation that the same man might be doing which requires a different amount of fatigue. I do believe in job evaluation, that fatigue should be a factor, but that only influences the base rate, but that may be a cent or two. When you get into some heavy work, it has to influence the earnings a little more than a cent or two, otherwise your incentive, earnings will not be consistent on your various jobs.

MEMBER: We are using values applied to each element. We found that the average, for instance, on core work for fatigue allowances works out to about 15 per cent. Of course, that will vary on large molds as you have found. On the large molds it will run more than that. The elemental values that we apply have been as high as 13 or 14 per cent. On the average, they

did work out to 15 per cent.

There was an elemental value you covered, your unmeasurable element. You applied that the same as you applied fatigue, on an elemental basis. Would you not think that would be added on after you get your normal standard on the basis of the overall studies that you make?

Mr. Annich: I like the idea of not multiplying the various

MEMBER: I see your point. It is just that I do not think, at least in our application, we would attempt to add to any particular element.

If you are going to apply fatigue by elements, you are going to handle indefinable duties or unmeasurable duties as a separate element in itself.

MR. ANNICH: The main thing is that you can add your personal right in with the fatigue and forget it. Some of the plants reported their values with the personal allowance included and I had to pull it out. All the values in the report are straight fatigue allowances.

MEMBER: Did any of the companies who submitted data to your Committee use spellmen around the molding line?

Mr. Annich: Only one or two companies did that. MEMBER: We have added some spellmen to our molding lines and the question came up as to whether we should reduce the fatigue allowance. We decided not to and we were glad that we decided that way because the resultant increase in efficiency was right up there.

MR. Annich: It was mentioned that when there were large enough flasks, help was supplied to carry them out. If you had a heavy flask which weighed 150 or 200 lb and you did not have a hoist to handle it, you have to have a man help carry it out or lift it if necessary. Personally I feel that if we have operations like that we should try to mechanize in such a way that we do not have to have the extra man.

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SECTION SIZE RELATIONSHIPS IN NODULAR IRON

By

G. Vennerholm, H. Bogart, R. Melmoth*

THROUGHOUT METALLURGICAL and engineering circles interest in a realistic evaluation of the true place of nodular iron in the ferrous castings picture continues to run high. Many investigators, including ourselves at Ford, have shown that it is possible to produce consistently nodular iron with most of the proposed magnesium addition agents, but there remains a paucity of service and field data to support the position of the more enthusiastic advocates of nodular iron. Likewise, the many gaps that exist in the basic data explaining the mechanism of the formation of the nodules retard the progress toward production application of nodular iron. A major deterrent to the extensive use of this material is the need for exacting control of the manufacturing process, a situation which arises primarily from this ignorance of the mechanism involved in the process. We have been able to determine experimentally that, under given conditions of analysis, temperature, type and method of addition of magnesium alloy and inoculant, an iron of consistently high properties can be produced, but the extension of any one of these factors into unexplored fields requires a new look at the overall process.

Not unlike any other casting alloy there is, at the same time, the necessity of translating test bar results from the laboratory to the repetitive production casting. Suffice to say that the job at the foundry is to make and sell castings-not test bars. With this in mind, our earlier work was earmarked directly for those castings where nodular iron seemed to have a more potent application. However, it has been readily apparent that the measure of the applicability of a nodular iron can not be determined by testing a 1-in. test bar, or even by examining the microstructure of the part in question but rather by studying the influ ence of the various items of chemistry on the section size sensitivity of nodular iron. To this end we have begun to make initial observations as to the influence of magnesium residuals on the common physical properties in the two materials previously referred to by the writers in the paper on "Nodular Iron," presented before the Society of Automotive Engineers, on Jan. 13, 1950, as high strength nodular iron and high ductility nodular iron.

The procedure adopted for accumulating data for this investigation may be divided into three phases:

- Production of test castings in various section sizes.
- Heat treatment of samples from these castings, and
- 3. Physical and metallurgical testing of samples.

Open-risered dry sand molds were used for all the test castings. The 1-in. sections studied were cut from the bottom bars of 11/4-in. keel blocks; in the larger sections, the samples were cut from the center portion of adequately risered 2 in. x 2 in. x 10 in., 4 in. x 4 in. x 10 in., 6 in. x 6 in. x 10 in., and 8 in. x 8 in. x 10 in blocks which were cast horizontally. A complete set of test castings for each magnesium level to be investigated was poured from the same ladle of metal and allowed to cool in the mold to a temperature somewhat below 1300 F. The pig iron for all heats involved was taken from stock of known analysis, and precautions were taken to hold variations in the ladle composition to a minimum. The heats were made in a 300-lb direct-arc electric furnace and tapping temperatures were maintained at 2580 to 2625 F (optical). The magnesium was added to the stream as 6 per cent magnesium-copper-ferrosilicon alloy, screened 1/2 in. x 30 M, and inoculated with 0.30 per cent silicon as 75 per cent silicon-ferrosilicon.

Types of Nodular Iron Studied

Two types of nodular iron were investigated, one a high strength iron made from a basic blast furnace pig, and the other a high ductility iron made from a special electric furnace pig. The desired final analysis range for these irons is tabulated below:

| | C | Mn | Si | P | S | Cu |
|-----------|-----------|-----------|-----------|-----------|-------|-----------|
| High | | | | | | |
| Strength | 3.50-3.80 | 0.90-1.10 | 2.00-2.40 | 0.10-0.18 | 0.015 | 0.10-0.50 |
| High | | | | | | |
| Ductility | 3.50-3.80 | 0.20-0.30 | 2.50-3.00 | 0.03 Max | 0.015 | 0.10-0.50 |

Eight heats of high strength iron and six of high ductility iron were made at magnesium levels varying

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from 0.025 to 0.09 per cent and a set of specimens of varying section sizes were cast from each. The average analyses of the two groups were as follows:

| | | C | Mn | Si | P | S | Cu |
|------|-----------|------|------|------|-------|-------|------|
| High | Strength | 3.78 | 0.92 | 2.30 | 0.12 | 0.012 | 0.42 |
| | Ductility | 3.79 | 0.25 | 2.73 | 0.023 | 0.011 | 0.43 |

Magnesium analysis was performed spectrographically using \(\frac{5}{32} \)-in. diameter pins cast in a two-piece iron mold to insure solid white iron samples. (Refer to publication by F. Bryan, G. Nahstoll, H. D. Veldhuis, entitled "Spectrographic Methods for Determining Magnesuim in Nodular Iron," ASTM Bulletin 162,69, Dec. 1949). Two sets of bars (1 in. x. 1 in. x 6 in.) were cut from each specimen as outlined above, so that physical property determinations and microscopic examinations could be made both in the ascast and annealed conditions. Annealing was accomplished by heating to 1750 F holding at tempreature 2 hr, and cooling in the furnace. Standard 0.505-in. diameter test specimens were machined from each test bar and ultimate strength, yield point, elongation, reduction of area, Brinell hardness and modulus of elasticity were determined for each section size at the various magnesium levels in order to evaluate the influence of magnesium content on general physical properties and on section sensitivity. In addition, representative 0.798-in. diameter unnotched 0.866-in. cantilever impact specimens were prepared and tested using a 264 ft-lb blow.

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The accumulated physical data was then plotted against magnesium residuals as may be seen in the accompanying graphs. Samples for metallographic study were cut from the broken tensile test specimens.

We would like to subdivide our data as it relates to the two types of nodular iron previously mentioned, namely, high strength and high ductility nodular iron.

High Strength Nodular Iron

Figure 1 is the representation of the section sizetensile strength-magnesium residual relationships in as-cast high strength iron. In all cases those relationships at the lower percentages of magnesium representing magnesium additions insufficient to produce nodular iron have not been completely investigated so that the probable values as indicated by dotted lines have been used. It may be seen from the graph that the tensile strength generally remains constant after a completely nodular structure is obtained. Some tendency to reach a maximum around 0.08 per cent magnesium is noted. The increase of section size results in a decreased ultimate strength although the nodular graphite formation was only slightly affected as evidenced by the microscopic examinations.

In a similar manner, Fig. 2 demonstrates the generally upward trend of the yield strength after the first appearance of graphite nodules as a structural constituent. Again the loss of strength as the section size increases is apparent. Figure 3 shows that the elongation in this high manganese medium phosphorus nodular iron does not appear to be affected by either section size or by magnesium content as in all cases investigated the elongation was approximately 1 per cent. No attempt has been made to correlate reductions of area since this property so closely parallels the elongation obtained in nodular iron.

In order to study the response of these same irons in varying section size to annealing treatments, a heat treatment designed to develop the maximum ductility consistent with reasonable production practice was employed. This consisted of heating the specimens to 1750 F-holding for 2 hr and furnace cooling. The graphic interpretation of this data is shown in Fig. 4, 5, and 6. Examination of Fig. 4 shows that after the truly nodular iron is formed, the ultimate strength increases substantially with increasing magnesium and decreases as might be expected with increased section size. Moreover, the ultimate strengths of the heat treated materials are generally above those of the not heat treated materials when 0.065 per cent magnesium is exceeded; this is especially true of the heavier sections at lower magnesium levels and becomes true of the lighter sections as the magnesium is increased.

Figure 5 shows the manner in which the yield points definitely increase with increasing magnesium but, unlike the ultimate strengths, the yield points of the heat treated material do not exceed those of the as-cast

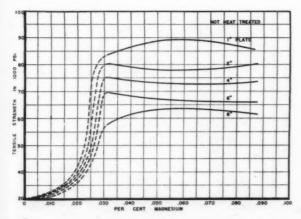


Fig. 1-Section size-tensile strength, magnesium residual relationship in high strength nodular iron.

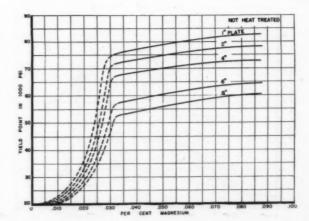


Fig. 2-Section size-yield point-magnesium residual relationship in high strength nodular iron.

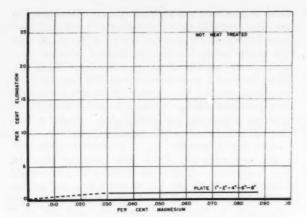


Fig. 3-Section size-elongation-magnesium residual relationship in high strength nodular iron.

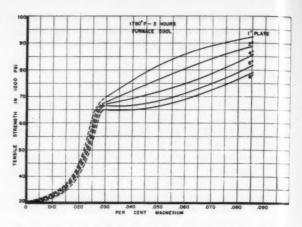


Fig. 4—Section size—tensile strength—magnesium residual relationship in high strength nodular iron.

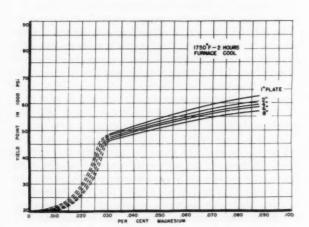


Fig. 5-Section size-yield point-magnesium residual relationship in high strength nodular iron.

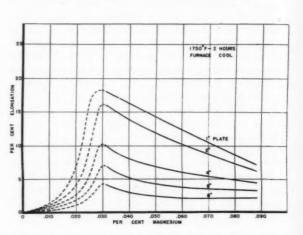


Fig. 6-Section size-elongation-magnesium residual relationship in high strength nodular iron.

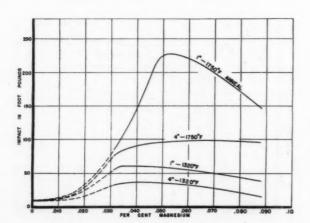


Fig. 7-Section size-impact-magnesium residual relationship in high strength nodular iron.

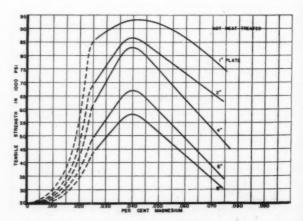


Fig. 8-Section size-tensile strength-magnesium residual relationship in high ductility nodular iron.

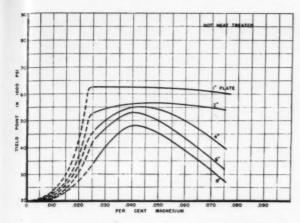


Fig. 9-Section size-yield point-magnesium residual relationship in high ductility nodular iron.

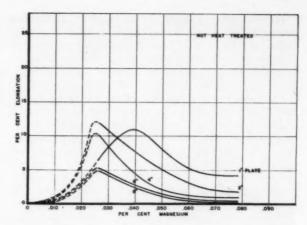


Fig. 10-Section size-elongation-magnesium residual relationship in high ductility nodular iron.

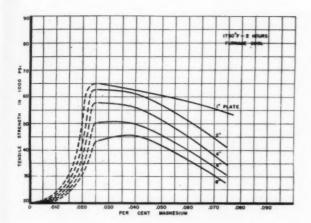


Fig. 11-Section size-tensile strength-magnesium residual relationship in high ductility nodular iron.

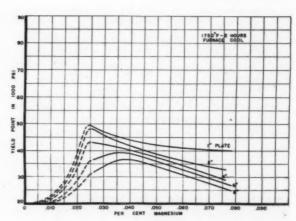


Fig. 12-Section size-yield point-magnesium residual relationship in high ductility nodular iron.

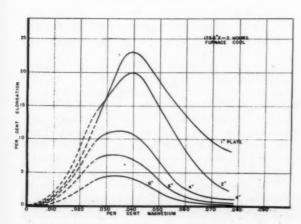


Fig. 13—Section size—elongation—magnesium residual relationship in high ductility nodular iron.

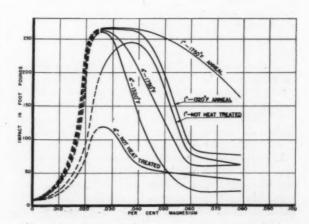


Fig. 14-Section size-impact-magnesium residual relationship in high ductility nodular iron.

material. The ratio of the yield point to tensile strength thus changes from 0.85 to 0.92 for the as-cast material, to 0.64 to 0.70 for the heat treated material. The most pronounced effect of this heat treatment is upon the ductility where, as shown in Fig. 6, the ductility rises rapidly to a maximum at 0.030 per cent magnesium many times that of the as-cast ductility; the family of curves representing the ductility then falls at a maximum rate of 2 per cent elongation for each 0.01 per cent magnesium. Again definite evidence of section sensitivity results in decreases of elongation from 1-in. to 8-in. sections.

Data on the impact relationships for the treated high strength iron presented in Fig. 7 shows that the impact rises rapidly reaching a high point at about 0.050 per cent magnesium and falls almost as rapidly as it rises. When a less drastic annealing treatment is used (i.e., heat to 1325 F—hold 2 hr—air cool) the impact strength drops well over 50 per cent. It is very noticeable that the proportion of loss in impact from 1-in. to 4-in. section is somewhat greater than the corresponding loss in ductility.

High Ductility Iron

In the case of the so-called high ductility iron, a similar graphic analysis (Fig. 8, 9, and 10) of the various physical properties obtained with the as cast material shows:

1. The tensile strength rises rapidly until a maximum is reached at 0.042 per cent magnesium and falls rapidly above this magnesium level. The falling off of properties with increased section size is marked (Fig. 8)

2. The yield point likewise rises abruptly and reaches a maximum at the same point; however, the 1-in. and 2-in. sections do not exhibit the same tendency to fall as rapidly as do the yield points of the larger sections and the tensile strengths of all sections

(Fig. 9).

3. The data on elongation indicates that the ductility of the 2-in., 4-in., 6-in., and 8-in. sections rise very rapidly to a maximum and immediately begin to retreat, whereas the 1-in. section does not reach a maximum ductility until 0.040 per cent magnesium is reached at which point it assumes the general characteristics of the family of curves (Fig. 10).

The analysis of this same material after heat treat-

ing at 1750 F-2 hr-air cool, shows:

1. The tensile strength rises rapidly and reaches an early maximum at 0.028 to 0.038 per cent magnesium residual and then falls off gradually, at a more accelerated rate in the larger cross sections (Fig. 11).

2. The yield point rises to the same early maximum as does the tensile strength and diminishes gradually as the magnesium content is increased (Fig. 12).

3. The elongation increases rapidly until a maximum is reached at 0.035 to 0.040 per cent magnesium and diminishes until at 0.080 per cent magnesium the 4-in., 6-in. and 8-in. sections do not show any appreciable ductility (Fig. 13).

The impact strengths of the so-called ductile iron is expressed in Fig. 14 from which it is readily seen

that:

1. The as-cast impact rises rapidly in the 1-in. sections until 0.038 per cent magnesium is reached and falls off very rapidly, while the impact in the 4-in. sections is in decline above 0.025 per cent magnesium.

2. The impact strengths of the various annealed sections rises rapidly and falls off quite rapidly as the magnesium level exceeds 0.04 per cent for the 1-in. section and 0.025 per cent for the 4-in. section.

3. The use of an annealing temperature above the critical and slow cooling has substantially improved the resistance of the material to impact loading as measured by this test.

Microstructural Analysis

We would like to analyze the data presented at additional length through the use of representative photomicrographs Fig. 15, 16, and 17 showing the microstructure of 1-in., 2-in., and 8-in. samples at the low end of the magnesium scale (0.030 per cent) before annealing. We see clearly that this material is already almost 100 per cent nodular graphite in a strong pearlitic matrix. Figures 18, 19, and 20 (1-in., 2-in., and 8-in. samples at 0.075 per cent magnesium) show that the metallurgical structure is rather consistent over the entire magnesium range even though the properties have deteriorated. A somewhat closer scrutiny of Fig. 20 shows that the nodules have a tendency to have protuberances about them instead of the conventional entirely spherical sphere. Figures 21, 22, and 23 are micrographs of the 1-in., 2-in., and 8-in. samples at the low end of the magnesium range after heat treatment; the manner in which the pearlite has been decomposed explains readily the high ductility, while the presence of more persistent pearlite in the higher magnesium levels seems to account for the lower ductility.

A similar examination of Fig. 24, 25, 26, 27, 28, 29, and 30 supplies metallographic evidence of the behavior of the high ductility iron. The substantial amounts of ferrite with good nodules of carbon at the lower magnesium contents is responsible for the rapid increase in ductility. It should be noted that the 1-in sample in the low magnesium sample has appreciably larger quantities of pearlite than the larger sections resulting in the lower ductility shown in Fig. 10 and 13. These larger amounts of pearlite are undoubtedly associated with the increased cooling rate of the 1-in test bar in the mold.

The rather rapid decay in all physical properties at magnesium contents in excess of 0.050 per cent in the case of the larger section sizes is related to the appearance of eutectiform graphite, as shown in Fig. 31, 32, and 33. By variations in the residual chemistry of the metal we have not been able to minimize the occurrence of this undesirable graphite formation but have not made material changes in the ductility. Some differences in the strengths have been encountered.

The annealing heat treatment as in the high strength iron causes additional breakdown of the pearlite into ferrite and additional graphite. Figures 34, 35, and 36 show the almost complete breakdown to ferrite and graphite in the 1-in., 2-in. and 4-in. samples at the 0.026 per cent magnesium level. Exam-

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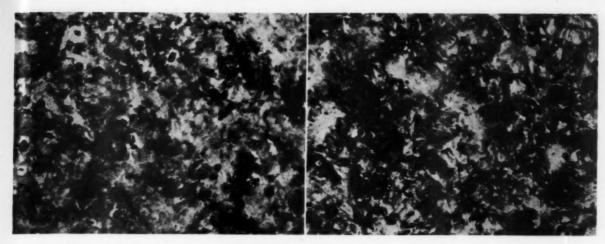


Fig. 15—High strength nodular iron, 0.030 Mg, as-cast, 1-in. section.

Fig 16-High strength nodular iron, 0.030 Mg, as-cast 2-in. section.

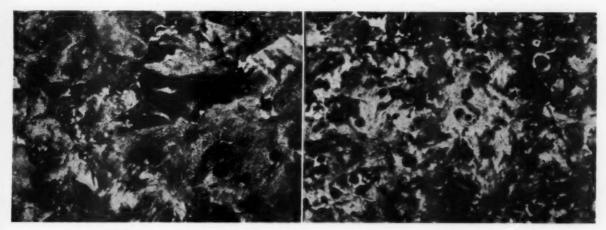


Fig. 17—High strength nodular iron, 0.030 Mg, as-cast, 8-in. section.

Fig. 18-High strength nodular iron, 0.075 Mg, as-cast, 1-in. section.

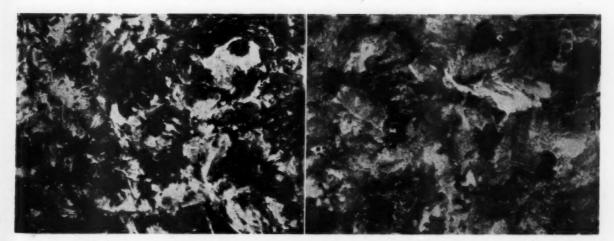


Fig. 19-High strength nodular iron, 0.075 Mg, as-cast, 2-in. section.

Fig. 20-High strength nodular iron, 0.075 Mg, as-cast, 8-in. section.

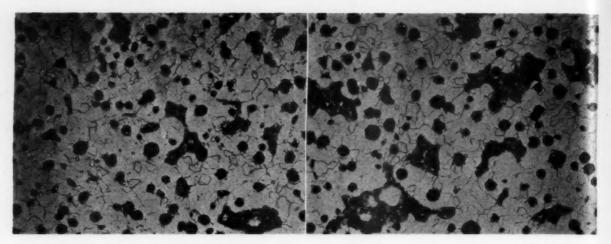


Fig. 21-High strength nodular iron, 0.030 Mg, annealed, 1-in. section.

Fig. 22-High strength nodular iron, 0.030 Mg, annealed, 2-in section.

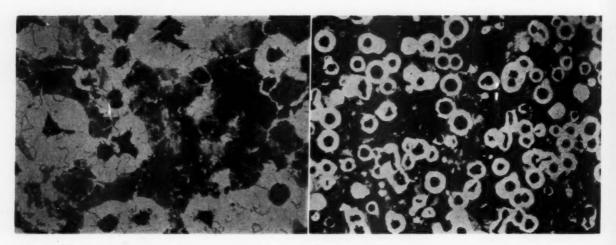


Fig. 23—High strength nodular iron, 0.030 Mg, annealed, 8-in. section.

Fig. 24--High ductility nodular iron, 0.026 Mg, as-cast 1-in. section.

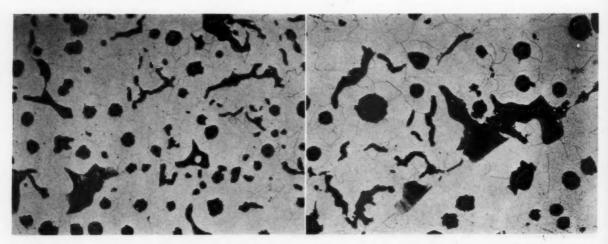


Fig. 25-High ductility nodular iron, 0.026 Mg, as-cast 2-in. section.

Fig. 26—High ductility nodular iron, 0.026 Mg, as-cast, 4-in. section.

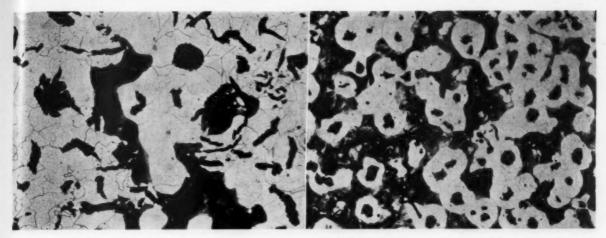


Fig. 27—High ductility nodular iron, 0.026 Mg, as-cast, 8-in. section.

Fig. 28-High ductility nodular iron, 0.040 Mg, as-cast, 1-in. section.

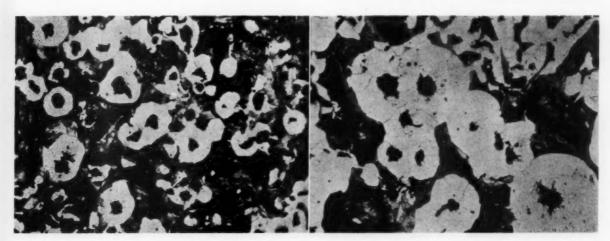


Fig. 29—High ductility nodular iron, 0.040 Mg, as-cast, 2-in, section.

Fig. 30-High ductility nodular iron, 0.040 Mg, as-cast, 8-in. section.

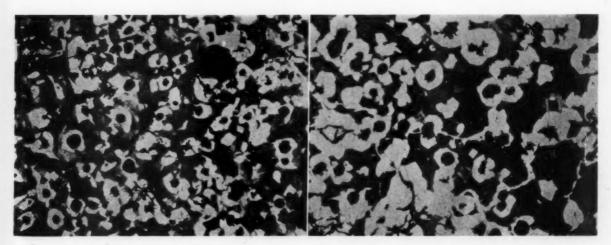


Fig. 31—High ductility nodular iron, 0.078 Mg, as-cast, 1-in. section.

Fig. 32-High ductility nodular iron, 0.078 Mg, as-cast, 2-in. section.

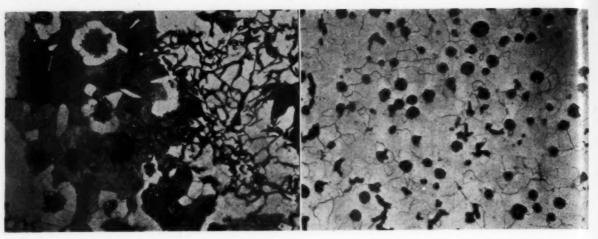


Fig. 33—High ductility nodular iron, 0.078 Mg, as-cast, 8-in. section.

Fig. 34—High ductility nodular iron, 0.026 Mg, annealed, 1-in. section.

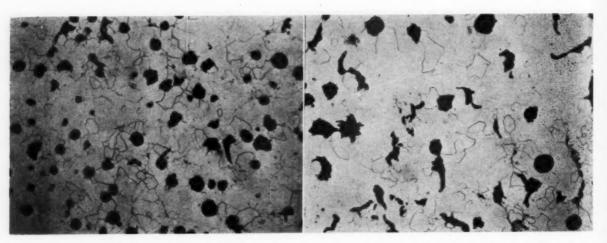


Fig. 35-High ductility nodular iron, 0.026 Mg, annealed, 2-in. section.

Fig. 36-High ductility nodular iron, 0.026 Mg, annealed, 4-in. section.

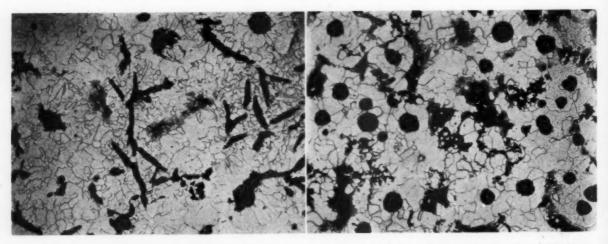


Fig. 37—High ductility nodular iron, 0.026 Mg, annealed, 8-in. section.

Fig. 38-High ductility nodular iron, 0.079 Mg, annealed, 1-in. section.

ination of Fig. 37 shows the tendency for increased amounts of persistent pearlite in the 8-in. section. Figures 38 and 39 again show the presence of the more persistent pearlite in the 1-in. and 8-in. sections at 0.079 per cent magnesium.

To make a direct comparison of the data presented with the classical data in gray iron may be of some interest. In Fig. 40 we have reproduced the interpretation of the section size sensitivity of gray iron from the paper by Schneidewind and McElwee published in this volume and superimposed upon their data some of the findings from our nodular iron work. It may be seen that the slopes of the nodular iron curve vary from roughly parallel; i.e., similarly sensitive to size variation to significantly steeper—in other words, more sensitive to size variation.

We would like to reconsider for a moment the strength and ductility relationships in the as-cast highstrength iron and add to this data some preliminary machining data. Figure 41 shows the manner in which the relative machinability decreased rapidly as magnesium is added to form nodular iron and continues to decrease as the iron is over treated. If, however, we assume that ± 0.015 per cent magnesium is the practical control limit in the use of nodular iron, we see that the relative machinability decreases from 35 to 20, a drop of 40 per cent. If this same sort of decrease in machinability may be expected in other grades and treatments of nodular iron along with the decline in other physical properties, notably ductility, it is apparent that although we can guarantee to make iron with nodular graphite, we may not find it practical to make the most ductile, the most machinable nodular iron on production.

The preliminary look that we have had at the section size sensitivity of nodular iron has convinced us of the desirability of establishing its relationship to other items of chemistry other than magnesium and

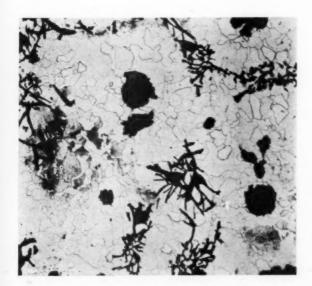


Fig. 39—High ductility nodular iron, 0.079 Mg, annealed, 8-in. section.

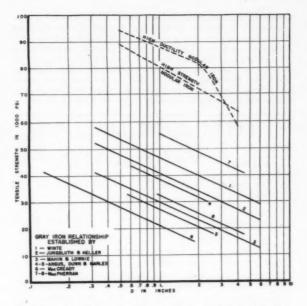


Fig. 40—Tensile strength as influenced by section size, cast in sand.

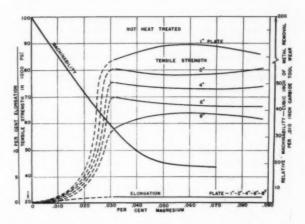


Fig. 41—Relationship of physical properties and magnesium residual in high strength nodular iron.

of obtaining still additional basic data on nodular iron before it is ready as an item of commerce, except on isolated specialty items.

DISCUSSION

Chairman: R. G. McElwee, Vanadium Corp. of America, Detroit.

Co-Chairman: V. A. CROSBY, Climax Molybdenum Co., Detroit.

J. T. MacKenzie: If I understand correctly, these magnesium analyses were determined on a separately cast specimen. We have analyzed 3-in. test bars and found as much as double the amount of magnesium in the top half as the bottom half of the bar. I would be very dubious if they were not checked on the same specimens as the tests were run on.

Mr. Bogart: We did not determine these results from pieces integral with the test specimens. They were determined on

¹ Technical Director, American Cast Iron Pipe Co., Birmingham, Ala.

separately cast test pins for this reason: that contrary to Dr. MacKenzie's experience we have had less difficulty in obtaining comparative results with white cast iron specimens than with irons that we have allowed to become pearlitic without going through a rather long and involved chemical separation. All of our samples were analyzed spectrographically using chill-cast, white iron specimens. I will say that we obtained a few verification points from time to time using a spectrochemical procedure and the results do not seem to be out of line, as shown.

| | | Fo | ord | Do | w* |
|-----|-----------------|----------------------|---------------------|----------------------|---------------------|
| San | nple | Spectro- chemical | Spectro- graphic | Spectro- chemical | Spectro- graphic |
| Al | Test Bar | 0.068 | 0.054 | 0.061 | |
| Al | Chill Cast Pins | | | 0.076 | 0.065 |
| A2 | Test Bar | 0.064 | | 0.068 | |
| A2 | Chill Cast Pins | | | 0.076 | |
| В | Test Bar | | 0.053 | 0.053 | |
| B | Chill Cast Pins | | | 0.061 | 0.066 |
| C | Chill Cast Pins | | | 0.050 | |
| C | Test Bar | 0.038 | 0.038 | 0.044 | |

W. K. Bock: ^a Recently some experiments were carried on by Prof. DeSy in Belgium and I believe he found the retained magnesium was a function of section size. Had the authors analyzed the different sections, different results would have been obtained. It might influence the shape of some of the curves given in such a way as to make less marked the sudden changes from curves which are concave upwards to curves concave downward.

MR. BOGART: We did not attempt to idealize our data. We would like to present a beautiful set of families of curves in

each instance, but the data did not always lend itself to such a relationship.

R. A. FLINN: ⁸ We wish to congratulate the authors on a fine and very extensive piece of work. One of our questions pertains to machinability. The 20,000-lb iron shows a factor of 100 or very good machinability. The unheat-treated ductile or nodular iron shows poor machinability. What was the percentage of ferrite in the 20,000-lb iron as compared to heat treated iron? I think the unheat-treated iron was practically all ferritic. That is, how much decline in machinability was due to per cent ferrite vs per cent pearlite and how much to flake graphite vs nodular graphite?

The other question was on the falling off in properties with increased retained magnesium. Do the authors believe this was due to improper graphite structure or perhaps to a carbide network formed during cooling from 1750 F?

Mr. Bogart: That is correct.

Mr. FLINN: We have noticed this falling off in properties with increased magnesium in one particular case and it did not seem to be due entirely to the amount of carbide there. There certainly is an effect and we would very much appreciate your comments on it.

Mr. Bogart: In the first part, I must agree, there is a definite change in going from 20,000-lb iron, to say, 40,000-lb iron, i.e., from a ferritic to pearlitic iron. That may account for at least half of that variance in the machinability. We were not interested in showing the decline there except to call to your attenion the fact that after we got within the boundary where the iron became nodular the loss in machinability continued as the magnesium was increased. On the matter of falling off of the properties in the higher magnesium levels, I tried to suggest that we have duplicated the results using another type of pig iron with a more normal graphite structure without substantially improving the properties.

Dr. MacKenzie: Was there any difference in titanium content

Mr. BOGART: It happened that there was a slight variation in the amount of titanium in the two pig irons, but it did not seem significant and we are searching for additional reasons for this variation in structure.

³ Research Engineer, National Malleable and Steel Castings Co., Cleveland.

⁸ Metallurgist, American Brake Shoe Co., Mahwah, N. J.

THE CONTRIBUTION OF RISER AND CHILL-EDGE EFFECTS TO SOUNDNESS OF CAST STEEL PLATES

By

H. F. Bishop* and W. S. Pellini**

ABSTRACT

The size of cast steel plates which can be made radiographically sound without resource to padding or chills has been determined for various riser and chill edge conditions. Temperature measurements were also made in plate sections in order to relate casting soundness to thermal gradients. It was found that for steels containing 0.20 to 0.35 per cent carbon, the largest plate section having a width-to-thickness ratio of 3 to 1 or greater which can be made sound with a single riser is one for which the distance from the perimeter of the riser to the edge of the casting is equal to 41/2 times the casting thickness. Of this distance, 21/2 times the casting thickness is made sound by the chilling effect of the casting edges. These findings apply to plates fed by risers having diameters equal to, or greater than, three times the casting thickness. The use of extra-large risers on plate castings was found to have no measurable effect on the distance which could be made sound.

The riser and chill-edge relationships result from the development of three thermal zones during the solidification of plate castings: (1) a gradient zone adjacent to the riser, (2) a gradient zone at the casting edges, and (3) an intermediate zone between these two which is essentially without a gradient. As solidification progresses, the two gradient zones grow toward each other at the expense of the intermediate zone. Complete soundness will be obtained if these zones can join or can at least reduce the intermediate region to a width less than the casting thickness before solidification is complete.

THE BASIC REQUIREMENT FOR SOUNDNESS in a casting is the presence of proper temperature gradients to insure directional solidification. If such gradients are present, risers can promote feeding for great distances, otherwise feeding will be negligible. Thus the riser is primarily a reservoir of molten metal with great potential feeding ability which is governed by thermal conditions determined not only by the riser per se but also by other factors, such as the thickness of the casting and the rate of heat extraction by the mold. This report is concerned with a study of factors which affect the solidification of cast steel plates of uniform section without padding or chills. Radiographic studies and thermal analyses were adopted to determine soundness and course of solidification respectively.

The investigation included circular sections representative of wide plates and rectangular sections representative of narrow plates. Radiographic studies were made with only one class of steel (0.20–0.30 C, 0.50–0.80 Mn, 0.35–0.55 Si) poured at 2900 to 2975 F. The steels for the thermal studies included a range of carbon contents (0.015 to 0.35 per cent) and were poured

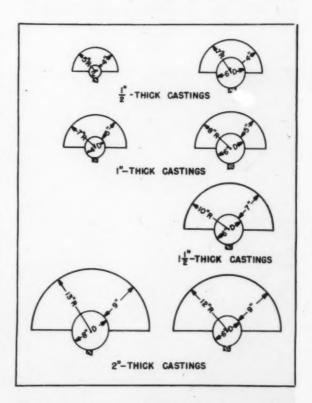


Fig. 1-Maximum sizes of semicircular plates of various thicknesses which can be cast sound without use of chills or padding.

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^{*} Metallurgist and ** Head, Metal Processing Branch, U. S. Naval Research Laboratory, Washington, D. C.

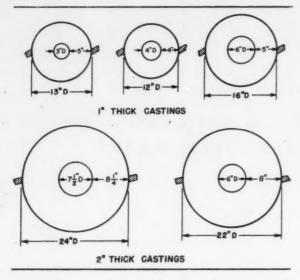


Fig. 2-Largest 1-in. and 2-in.-thick circular castings which can be made sound without use of padding or chills.

at 2950 plus or minus 25 F unless otherwise noted. All steels were melted in either a 300-lb or a 2000-lb induction furnace.

TABLE 1-SUMMARY OF TESTS MADE WITH SEMICIRCULAR PLATES

| Plate No. | | Plate Thick- ness, in. | Radius, | Distance to be Fed, in. | Volume | | |
|--------------|---|------------------------------|---------|-------------------------------|--------|--------|--------|
| J5 | 6 | 1/2 | 6 | 3 | 225 | Sound | |
| J6 | 6 | 1/2 | 7 | 4 | ** | Sound | |
| J2 | 6 | 1/2 | 8 | 5 | 11 | Shrink | |
| J1 | 6 | 1/2 | 10 | 7 | ** | Shrink | |
| T23 | 2 | 1/2 | 4 | 3 | 10 | Sound | |
| T22 | 2 | 1/2 | - 5 | 4 | 2.5 | Sound | |
| T21 | 2 | 1/2 | 6 | 5 | ** | Shrink | |
| T20 | 4 | 1 | 7 | 5 | 75 | Sound | |
| T18 | 4 | 1 | 73/4 | 53/4 | ** | Slight | Shrink |
| T17 | 4 | 1 | 81/4 | 61/4 | ** | Shrink | |
| T19 | 4 | 1 | 83/4 | 63/4 | | Shrink | |
| F 7 | 6 | 1 | 8 | 5 | 225 | Sound | |
| F5 | 6 | 1 | 9 | 6 | 22 | Shrink | |
| F6 | 6 | 1 | 10 | 7 | ** | Shrink | |
| G5 | 6 | 11/2 | 10 | 7 | 225 | Sound | |
| G3 | 6 | 11/2 | 11 | 8 | 22 | Slight | Shrink |
| G4 | 6 | 11/2 | 12 | 9 | ** | Shrink | |
| G2 | 6 | 11/2 | 14 | 11 | ** | Shrink | |
| N14 | 6 | 2 | 12 | 9 | 225 | Sound | |
| N15 | 6 | 2 | 14 | 11. | ** | Shrink | |
| N16 | 6 | 2 | 16 | 13 | 90 | Shrink | |
| Г25 | 8 | 2 | 14 | 10 | 600 | Slight | Shrink |
| Г26 | 8 | 2. | 15 | 11 | ** | Shrink | |
| Т24 | 8 | 2 | 151/2 | 111/2 | ** | Shrink | |



Fig. 3-Radiograph of an area adjacent to rim of a 2-in.-thick circular casting 36 in. in diam fed with riser 71/2 in. in diam.

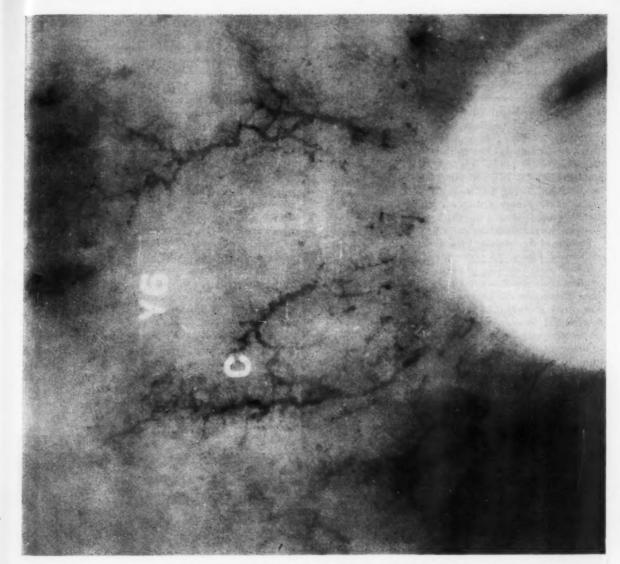


Fig. 4—Radiograph of area adjacent to the riser of a 2-in.-thich circular casting 36 in. in diam fed with a riser 7½ in. in diam.

Radiographic Studies

Feeding of Circular Sections Representative of Wide Plates Semicircular plates in thicknesses of 1/2 in., 1 in., 11/2 in. and 2 in. were cast with the riser located at the center of the straight edge of the casting, and with the gate attached to the riser as shown in Fig. 1. In all cases the height of the riser was approximately 11/2 times the riser diameter. Several series of castings of each thickness were made with approximately 1-in. variations in radii and, with the exception of the 11/2in. thick plates, with two different riser diameters. Pertinent dimensions and the results of the X-ray tests are listed in Table 1. In this and subsequent tables, feeding distance is the distance between the perimeter of the riser and the edge of the casting. In evaluating casting quality, a casting was considered sound only if there was no evidence of shrinkage detected by

radiography at $1\frac{1}{2}$ per cent sensitivity. When there was doubt as to the presence of shrinkage, transverse X-rays were made of strips removed from the casting.

The maximum radial plate sizes which could be made sound for different thicknesses and with different riser sizes are illustrated in Fig. 1. For ½-in. sections the critical distance is 4 in.; for 1-in. sections, 5 in; for 1½-in. sections, 7 in.; and for 2-in. sections, 9 in. As radii of the plates are increased above these sizes, radial shrinkage develops near the riser, but a zone equal to approximately $2\frac{1}{2}$ times the casting thickness at the rim of the casting remains sound. With further increases in radii, the shrinkage area and the severity of the shrinkage increase but the width of the sound rim zone remains unchanged. This sound rim is due to the chilling effect of the casting edge. Thus, it is apparent that the chilling effect of

the casting edges is a major factor in promoting casting soundness.

Completely circular castings (Table 2) were made in thicknesses of 1 and 2 in. to check the effects shown by the semicircular plates. Each casting was fed by one riser located at the center and was gated at two opposite positions on the circumference. The largest circular castings which could be made sound are illustrated in Fig. 2. Castings which were made larger than those of Fig. 2 have a sound rim but contain shrinkage extending close to the riser as was found in the semicircular plates. Figures 3 and 4 show radiographs of typical rim and riser zones respectively. It can be noted in Fig. 4 that shrinkage actually extends to the riser. Figure 5 shows a radiograph of a complete segment of a 1-in. thick circular plate of a diameter larger than the critical.

Feeding of Rectangular Plate Castings Representative of Narrow Plates—In order to determine the lower limits of plate behavior in regards to feeding (i.e. the smallest plate width which can be made sound for the same distance as the larger circular plates) a series of 2-in. thick rectangular castings having widths of 10, 8, 6, and 4 in. were made in various lengths. The plates were cast horizontally and fed with risers 6 in. in diameter and located on one end with gates leading into the risers. It was found (Table 3 and Fig. 6) that the distance which can be made sound in plates of 10, 8, and 6-in. widths are in close agreement with the results obtained with the circular plates. The 4-in.

wide plates however, can be made sound for greater distances.

Since edge effects promote soundness for a distance of 2½ times the casting thickness, it would be expected that rectangular castings having width to thickness ratios of less than 5 to 1 would show different freezing and feeding characteristics than the wide plates because of overlapping of the chilling effects of the side edges. It is believed that the similarity of narrow and wide plates is due to the greater surface-area to volume ratio at the ends of the narrow plates, which acts to increase the solidification rate from the ends of the narrow castings.

Figure 7 presents a graphic compilation of data taken from Fig. 1, 2, and 6, and shows the distance which can be made sound in flat plate castings having a width-to-thickness ratio of 3-1 and greater. The solid bars in Fig. 7 represent the portions of the castings made sound by the chilling effect of the edge, while the cross-hatched zones represent the added portions of the castings made sound by the thermal effect of the riser. The distances represented by the hatched portions of the bars may be interpreted as the feeding range of the risers.

For both circular and rectangular castings an approximation of the largest plate casting which can be made sound, measured from the riser perimeter to the edge of the plate, is $4\frac{1}{2}$ times the plate thickness. This approximation is valid for all plate castings with the exception of the $\frac{1}{2}$ -in. thick castings. Since the

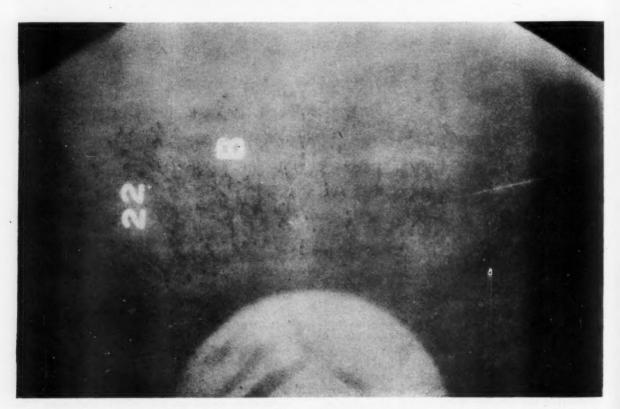


Fig. 5-Radiograph of section of circular casting 1 in. thick, 20 in. in diam, fed with a riser 6 in. in diam.

Note sound rim zone and extension of shrinkage to vicinity of riser.

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TABLE 2-SUMMARY OF TESTS MADE WITH

| Plate No. | Riser Diam, in. | Plate Thick- ness, in. | | Distance to be V Fed, in. | olume | |
|--------------|-----------------------|------------------------------|-------|---------------------------------|-------|---------------|
| X2 | 3 | 1 | 10 | 31/2 | 35 | Sound |
| X3 | 3 | 1 | 11 | 4 | ** | Sound |
| Pl | 3 | 1 | 12 | 41/2 | ** | Sound |
| P2 | 3 | 1 | 13 | 5 | ** | Sound |
| P4 | 3 | 1 | 14 | 51/2 | ** | Shrink |
| N17 | 3 | 1 | 14 | 51/2 | ** | Shrink |
| N18 | 3 | 1 | 153/4 | 63/8 | 34 | Shrink |
| N21 | 3 | 1 | 195/8 | 85/16 | | Shrink |
| Y1 | 4 | 1 | 10 | 3 | 75 | Sound |
| Y2 | 4 | 1 | 11 | 31/2 | ** | Sound |
| U2 | 4 | 1 | 12 | 4 | ** | Sound |
| U4 | . 4 | 1 | 13 | 41/2 | ** | Shrink |
| P6 | 4 | 1 | 14 | 5 | ** | Shrink |
| P5 | 4 | 1 | 15 | 51/2 | ** | Shrink |
| P3 | 4 | 1 | 16 | 6 | ** | Shrink |
| WI | 4 | 1 | 18 | 7 | ** | Shrink |
| W2 | 4 | 1 | 20 | 8 | ** | Shrink |
| S15 | 6 | 1 | 15 | 41/2 | 225 | Sound |
| P7 | 6 | 1 | 16 | 5 | ** | Slight Shrink |
| N20 | 6 | 1 | 16 | 5 | *** | Sound |
| \$16 | 6 | 1 | 17 | 51/2 | 5.5. | Shrink |
| N22 | 6 | 1 | 195/8 | 613/16 | ** | Shrink |
| 026 | 6 | 1 | 235/8 | 811/16 | ** | Shrink |
| P13 | 6 | 2 | 18 | 6 | 225 | Sound |
| P8 | 6 | 2 | 20 | 7 | ** | Sound |
| P11 | 6 | 2 | 22 | 8 | ** | Sound |
| Y5 | 6 | 2 | 24 | 9 | ** | Slight Shrink |
| 024 | 6 | 2 | 271/4 | 103/8 | ** | Shrink |
| Abl | 6 | 2 | 32 | 13 | ** | Shrink |
| P9 | 71/2 | 2 | 20 | 61/4 | 485 | Sound |
| P10 | 71/2 | 2 | 22 | 71/4 | ** | Sound |
| P12 | 71/2 | 2 | 24 | 81/4 | ** | Sound |
| Y3 | 71/2 | 2 | 26 | 91/4 | ** | Shrink |
| Y4 | 71/2 | 2 | 28 | 101/4 | ** | Shrink |
| Y6 | 71/2 | 2 | 36 | 141/4 | ** | Shrink |

TABLE 3—SUMMARY OF TESTS MADE TO DETERMINE MINIMUM SECTION WIDTHS WHICH BEHAVE AS PLATES

| Plate No. | Riser Diam, in. | Casting Length, in. | Distance to be Fed, in. | |
|--------------|-----------------------|---------------------------|-------------------------------|--------|
| | 2- | in. x 10-in. P | lates | |
| R9 | 6 | 145/8 | 85/8 | Sound |
| R10 | 6 | 151/2 | 91/2 | Sound |
| RII | 6 | 161/2 | 101/2 | Shrink |
| R12 | 6 | 181/2 | 121/2 | Shrink |
| | 9 | -in. x 8-in. Pla | | |
| AK4 | 6 | 13 | 7 | Sound |
| AK5 | 6 | 14 | 8 | Sound |
| AK6 | 6 | 15 | 9 | Sound |
| AN6 | 6 | 16 | 10 | Shrinl |
| AN4 | 6 | 18 | 12 | Shrinl |
| | 2 | -in. x 6-in. Pla | ates | |
| R5 | 6 | 145/8 | 85/8 | Sound |
| R6 | 6 | 151/2 | 91/2 | Sound |
| R7 | 6 | 161/2 | 101/2 | Shrink |
| R8 | 6 | 181/2 | 121/2 | Shrinl |
| | . 2 | in. x 4-in. Pl | ates | |
| R1 | 6 | 145/8 | 85/8 | Sound |
| R2 | 6 | 151/2 | 91/2 | Sound |
| R3 | 6 | 161/2 | 101/2 | Sound |
| R4 | 6 | 181/2 | 121/2 | Sound |

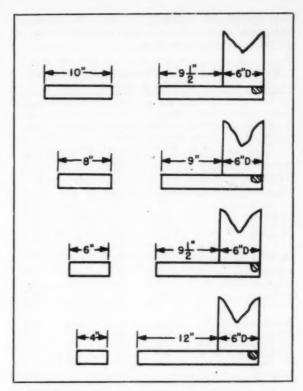


Fig. 6-Longest 2-in. thick rectangular castings of 10, 8, 6 and 4-in. widths which can be made sound with a 6-in. diam riser.

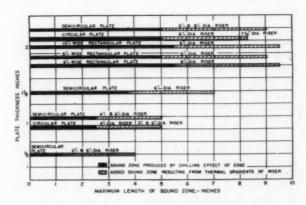


Fig. 7—Comparison of chilling effect of edge and heating effect of riser in producing soundness in flat plate castings having a width of thickness ratio of 3 to 1 and greater (Composite data from Fig. 1, 2 and 6).

chilling effect of the casting edges accounts for the feeding of a distance equal to $2\frac{1}{2}$ times the casting thickness, there remains a distance of 2 times the casting thickness which can be considered to be made sound by the riser. A quantitative value for the "feeding range" of a riser might then be a distance equal to 2 times the thickness of the section which it must feed. However, it should be emphasized that this applies only to completely sound castings. If the casting is

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large enough for shrinkage to form, the shrinkage will extend close to the riser and the feeding range of the riser will then be negligible.

TABLE 4-SUMMARY OF EFFECT OF RISER SIZE ON

| R | | Riser Diam, | | I | Feeding Distance to |
|---------------------------|-----------------------|--------------------------------|------------------------------|-------------------------------|-----------------------------|
| Plate Thickness in. | Riser Diam, in. | to Plate Thickness Ratio | Relative Riser Volume* | Distance to be Fed, in. | Plate Thickness Ratio |
| 1/2 Semicircular | 6 | 12T | 55V | 4 | 8T |
| 1/2 Semicircular | 2 | 4T | 2.5V | 4 | 8T |
| 1 Semicircular | 4 | 4T | 2.3V | 5 | 5T |
| 1 Semicircular | 6 | 6T | 7V | 5 | 5T |
| 1 Circular | 3 | 3T | 1V | 5 | 5T |
| 1 Circular | 4 | 4T | 2.3V | 4 | 4T |
| 1 Circular | 6 | 6. Γ | 7V | 5 | 5T |
| 11/2 Semicircular | 6 | 4T | 2V | 7 | 4.7T |
| 2 Semicircular | 6 | 31 | 1V | 9 | 4.5T |
| 2 Semicircular | 8 | 4T | 2.7V | 9 | 4.5T |
| 2 Circular | 6 | 3T | IV | 8 | 4T |
| 2 Circular | $71/_{2}$ | 3.8T | 2.1V | 81/4 | 4.2T |
| 2x10 Rectangular | 6 | 3T | 1V | 91/2 | 4.8T |
| 2x8 Rectangular | 6 | 3T | 1V | 9 | 4.5T |
| 2x6 Rectangular | 6 | 3T | 1V | 91/2 | 4.8T |
| 2x4 Rectangular | 6 | 3T | 1V | 121/2 | 9T |
| * Relative Volume | | ume of 3T | | | |

Effect of Riser Size on Feeding—Table 4 presents a summary of the effects of riser size on feeding range. It is noted that for risers of fixed geometry, (height equal to 1½ times the riser diameter) increasing the riser diameter above 3 times the plate thickness which is the smallest safe diameter for preventing underriser shrinkage, 1 does not result in an increase in the distance which can be made sound. These data suggest that the practice of using extra-large risers as a means of improving feeding of plate castings has no merit. The ineffectiveness of such practices is further emphasized by a consideration of relative riser volumes. Table 4 shows that increasing the riser volume by sevenfold over that of the 3 T riser has no beneficial effect.

Gating and Risering Practice as Related to Feeding-The effects of gate and riser location were studied in 2 x 8-in. rectangular castings which were shown to behave as plates and hence can be considered as sectional elements of plate castings of larger size. The dimensions and gating and risering variations studied listed in Table 5. The maximum lengths which can be made free of shrinkage with the different gating and risering systems are illustrated in Fig. 8. The gate location was found to have no measurable effect on the length which can be made sound. This may be due to the turbulence which motion picture studies2 have shown results from metal flowing into small molds of a simple shape. This turbulence probably equalizes temperatures sufficiently to make the heating effects of the gate negligible in the case of plate castings.

It can be noted from castings 1 through 7 of Fig. 8

TABLE 5-SUMMARY OF TESTS MADE TO DETERMINE EFFECTS OF GATE AND RISER LOCATIONS

| | | | Casting | Distar | ice | |
|-------|--------------|--------------------------|----------------|---------|------|--------|
| Plate | Riser | Gate | Length, | | | |
| No. | Location | Location | in. | Fed, i | n. | |
| | | lane, Vertica | | | in. | |
| AJ4 | Тор | Riser | 8 | - 8 | | Sound |
| AJ5 | Top | ** | 9 | 9 | | Sound |
| AM6 | Тор | ** | 10 | 10 | | Shrin |
| AM7 | Тор | ** | 11 | 11 | | Shrin |
| AM3 | Top | | 12 1. Diam. | 12 | 2 | Shrin |
| AJ8 | Top | Plane, Vertica Bottom | 7 | Diam, 6 | ın. | Sound |
| AJ9 | Тор | BOttom " | 8 | 8 | | Sound |
| AJ10 | Тор | ** | 9 | 9 | | Sound |
| AM10 | Тор | . 89 | 10 | 10 | | Shrin |
| AM9 | Тор | ** | 11 | 11 | | Shrin |
| AMII | Тор | ** | 12 | 12 | | Shrin |
| | | ne, Horizont | | | 6 ir | |
| AK1 | 1 End | Opp. Riser | 13 | 7 | 0 11 | Sound |
| AK2 | 1 End | " | 14 | 8 | | Sound |
| AK3 | -1 End | ** | 15 | 9 | | Sound |
| AN5 | 1 End | ** | 16 | 10 | | Shrink |
| AN2 | 1 End | 200 | 18 | 12 | | Shrink |
| AN3 | 1 End | 99 | 20 | 14 | | Shrink |
| | | ne, Horizonta | | | 6 in | |
| AK4 | 1 End | Riser | 13 | 7 | | Sound |
| AK5 | 1 End | ** | 14 | 8 | | Sound |
| AK6 | 1 End | ** | 15 | 9 | | Sound |
| AN6 | 1 End | ** | 16 | 10 | | Shrink |
| AN4 | 1 End | ** | 18 | 12 | | Shrink |
| ANI | 1 End | ** | 20 | 14 | | Shrink |
| | Casting Plan | ne, Horizonta | al; Riser | Diam, | 5 in | l. |
| AV15 | 1 End | Riser | 13 | 8 | | Sound |
| AV16 | 1 End | 300 | 14 | 9 | | Sound |
| AV17 | 1 End | ** | 15 | 10 | | Shrink |
| AV18 | 1 End | 99 | 16 | 11 | | Shrink |
| AV19 | 1 End | ** | 17 | 12 | | Shrink |
| | Casting Plan | | al; Riser | Diam, | 6 in | |
| AW5 | Center | Riser | 18 | 6 | | Sound |
| AW6 | ** | ** | 20 | 7 | | Sound |
| AW7 | ** | 9.5 | 22 | 8 | | Sound |
| AW8 | ** | ** | 24 | 9 | | Sound |
| AW9 | ** | ** | 26 | 10 | | Shrink |
| AW10 | ** | " | 28 | 11 | | Shrink |
| | | ne, Horizonta | | | 6 in | |
| AWII | Center | Ea. End | 16 | 5 | | Sound |
| AW12 | ** | ** | 18 | 6 | | Sound |
| AW13 | ** | ,,, | 20 | 7 | | Sound |
| AW14 | | ., | 22 | 8 | | Sound |
| AW15 | ** | 2.5 R2 | 24 | 9 | | Sound |
| AW16 | 91 | | 26 | 10 | | Shrink |
| | | ne, Horizonta | | | 6 in | |
| AV21 | Ea. End | Ea. Riser | 16 | 2 | | Sound |
| BA1 | 82 | 99 | 18 | 3 | | Sound |
| BA2 | ** | ** | 20 | 4 | | Sound |
| BA3 | 22 | 27 | 22 | 5 | | Shrink |
| AV23 | 22 | 22 | 24 | 6 | | Shrink |
| AV24 | | | 28 | 8 | c : | Shrink |
| | | e, Horizonta | | | 6 in | |
| BA5 | Ea. End | Center | 16 | 2 | | Sound |
| BA6 | ,,, | ** | 18 | 3 | | Sound |
| BA7 | 22 | ** | 20 | 4 | | Sound |
| BA8 | 9.9 | 99 | 22 | 5 | | Shrink |

that castings can be made sound for a distance of 9 in. from the perimeter of the 6-in. diam riser when the chilling effect of the edges at the casting extremities are operative. This agrees with the distances noted for the wide (circular) plates of the same thickness. However, if a riser is located at each end of the castings, as in No. 8 and 9, the sound expanse of metal per riser is greatly reduced. By comparing the distance

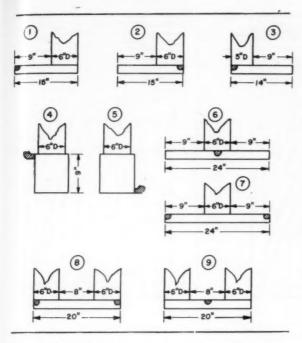


Fig. 8—Longest castings having 2-in. x 8-in. crosssection which can be made sound by various gating and risering methods.

of sound metal in castings No. 2 and 8, chill-edge and riser effects can be made evident. In casting No. 8, each riser, instead of feeding toward an edge of the casting actually feeds toward another riser, and the chilling effect of the casting edge is thus lost. The difference in the lengths of sound section between castings No. 2 and 8 is 5 in. The additional 5 in. of sound section is produced by the chilling effect the casting edge which is in agreement with the conclusions drawn from the circular plate castings.

The advantage of locating risers so that the beneficial edge effects are utilized to promote soundness is also apparent from casting 6 of Fig. 8. A casting 24 in. long can be made sound with a single 6-in.-diam riser located at its center. However, this same casting fed by two 6-in.-diam risers, one at each end, would produce only 20 in. of sound metal.

It is believed that it should be possible, utilizing the relationships presented, to feed large plates requiring multiple risers by locating the risers so that all locations on the plate are within the feeding influence of either a casting edge or a riser.

Thermal Studies

Thermal Gradients in Plate Castings—Although thermal gradients are always considered a prerequisite to casting soundness, so far as is known no actual temperature measurements have been made in solidifying steel castings to determine the extent of such gradients and their relation to feeding. Since the range of soundness in plate sections is governed by the chilling effect of the casting edges and the heating effect of the riser, it was considered essential to obtain

quantitative measurements of the thermal gradients developed by these two effects during the course of solidification.

Pt-Pt Rh (13 per cent) thermocouples were used to determine temperatures. The thermocouple wires (0.02-in. diam) were insulated by alundum tubing and protected by fused quartz tubes having an OD of $\frac{3}{16}$ in. and an ID of $\frac{1}{8}$ in. These tubes were inserted through the top of the mold into the mold cavity so that the thermocouple beads were at the center plane of the casting. Temperatures were recorded on a 16-point automatic potentiometer-recorder which permitted sequential temperature readings from 16 thermocouple stations in a period of approximately 30 sec.

Liquidus and solidus temperatures were obtained directly from inverse-rate cooling curves for most of the tests. Inverse rate curves were plotted on the basis of the time required for the temperature to drop 10 F. Couples located in shrinkage regions showed no well defined solidus; in such cases (Fig. 11A, for example), the solidus was approximated from the Fe–C equilibrium diagram.

The term solidus as used herein represents a "technical" solidus, considered as the temperature indicated by thermal analysis techniques (which are inherently not sufficiently sensitive to detect the last vestige of solidification) as the point at which delayed cooling is no longer observed. The technical solidus point therefore represents a condition of essential solidity except for thin, isolated films or pools of interdendritic liquid. Feeding must necessarily cease when such a condition is attained. The determination of an "absolute" solidus in preference to a "technical" solidus was not within the scope of this investigation and was moreover not necessary for the thermal analyses which are to be described.

Development of Near-Isothermal Plateaus in Plate Castings—Figures 9A, 9B, and 9C show the temperature gradients, at different time intervals after casting, across 8-in.-thick, semicircular castings having radii of 12, 14, and 16 in. respectively. Since half of the 6-in.-diam riser extends over the plates, the actual distance to be fed in each case is 3 in. less than the radius. The smallest of these castings was sound, the intermediate contained slight shrinkage, and the largest contained severe shrinkage.

It will be noted from the curves in Fig. 9 that the centerplanes of the castings pass through the primary liquidus temperature within a minute after pouring without measurable thermal gradients. Gradients, however, are soon established at each extremity of the castings. The risers, because of their mass and because of the re-entrant angle formed with the casting, reduce the rate of heat extraction, while the casting edges, because of the added surface area which they present, increase the rate of heat extraction. Short gradients are thereby established near these locations soon after entering the solidification range.

The riser gradients and casting edge gradients tend to progress toward each other in an attempt to effect a junction during the progress of solidification. This occurs at the expense of a central uniform temperature

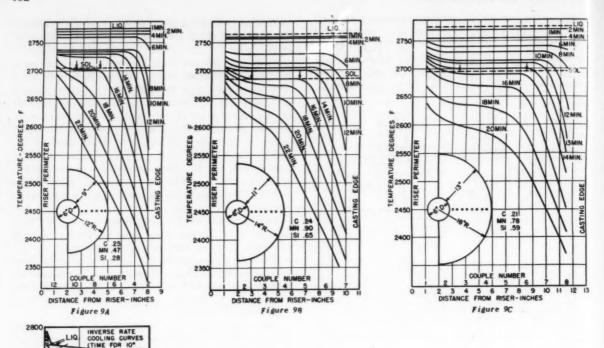


Fig. 9—Thermal gradients along center plane of 2-inthick castings at various intervals after pouring showing relation of length of near-isothermal zone to plate soundness. Casting of Fig. 9A is sound. Casting of Fig. 9B contains slight shrinkage. Casting of Fig. 9C contains severe shrinkage.

plateau.

It will be noted that instead of the usual smooth thermal gradients generally associated with heat flow, the gradients from the edge of the casting change abruptly as junction with the plateau is approached. The "knee" formed by this abrupt change is due to the necessity of extracting heat of solidification from this zone before the thermal gradients can move inward. The "knee" thus represents a narrow zone of rapid, progressive solidification.

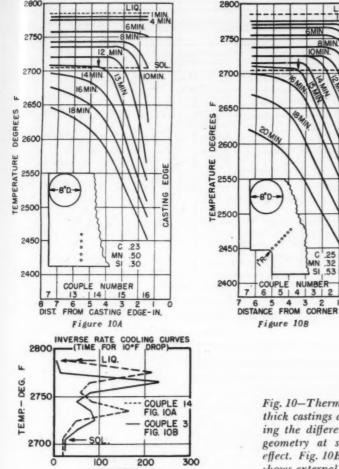
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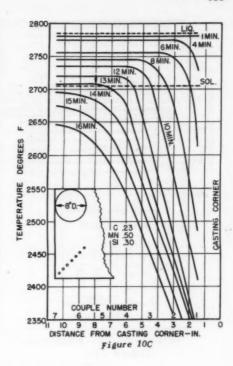
The inward progression of the edge gradients represents an example of the interesting and fundamental phenomenon which is basic to directional solidification conditions. To understand this phenomenon it is necessary to understand the heterogeneous nature of the freezing process particularly for materials such as steel which solidify over a temperature range. As the liquid metal is cooled below the liquidus, the first solid forms and solidification proceeds at a rate dependent on the rate of heat extraction. Since heat is extracted faster at the edge of the casting, solid particles will develop at a faster rate at this position. As the overall temperature level drops, the ratio of liquid to solid decreases; effectively there is less liquid remaining to solidify. Since heat of fusion is the primary source of heat evolved during solidification, and since the rate of liberation of such heat is strictly proportional to the rate and amount of solidification at any given instant, it follows that toward the end of solidification less and less heat will be liberated as the

amount of liquid remaining decreases. The rate of heat extraction at the casting edge, however, does not decrease in such a pronounced manner. It is therefore evident that as the liquid at any fixed point within the "knee" zone is gradually exhausted the thermal gradient at that point must necessarily become steeper. The "knee" will therefore progress inward past this point to maintain the dynamic heat balance characteristic of the progressive solidification process.

The riser end gradients are due to a somewhat different mechanism-flow of heat from the riser to the casting. As such these gradients depend primarily on the temperature differential between riser and casting. Since the riser cools more slowly than the casting this differential increases with time. This effect, together with the increased conductivity due to increased amount of solid in the casting, results in increased rate of heat conduction as solidification progresses, hence with the moving of the gradient inward.

The primary thermal effect of increasing the plate radii is to increase the expanse of the essentially isothermal zones existing when the solidus temperature is approached. These zones, indicated by arrows on the curves of Fig. 9, show that as the radii of the plates vary from 12 to 16 in. the near isothermal zone is increased from approximately 2 to 5 in. The length of this zone at the solidus is most important since it determines whether or not final freezing will be progressive. A maximum length of gradient less zone equal to IT (2 in. in 2-in. section) can be tolerated





TIME-SECONDS

Fig. 10-Thermal gradients along center plane of 2-in. thick castings at various intervals after pouring showing the different chilling effects resulting from plate geometry at sand interface. Fig. 10A shows edge effect. Fig. 10B shows internal corner effect. Fig. 10C shows external corner effect.

without causing shrinkage.

Riser, Edge, and Corner Effects-Gradients from internal and external corners as well as from the edge of a 2-in. thick casting are illustrated in Fig. 10. The inward movement of the casting edge gradient as solidification progresses is again evident in this series of curves. Because of the greater surface area to volume ratio of the metal at the external corner, the chilling effect is greater, and at the solidus temperature the distance from the corner to the near-isothermal plateau is increased to approximately 8 in. The metal at the internal corner has a much smaller surface-area to volume ratio, thus the distance from the corner to the plateau is decreased to approximately 31/2 in.

Studies of the edge and external-corner effects were repeated on a heavier section, a 4-in. thick slab casting 3 ft sq fed with a 12-in. diam, 18 in. high riser located at one of the corners. The thermal gradients at the edge and corner of this casting are shown in Fig. 11B and 11C. Measurements were made only at 3 or more inches from the casting edge and corner. No gradients were detected in the corner zone until after 8 min had elapsed and no appreciable gradients were present in the edge zone until at least 20 min after pouring. By the time the plateau region reached the solidus

temperature the chilling effect of the casting edge had caused pronounced thermal gradients to extend into the casting for a distance of approximately 10 in. Beyond this 10-in. distance the central plane passed through the solidus as a near-isothermal plateau thus leading to shrinkage. Along a line bisecting the external corner, thermal gradients extended into the casting for about 15 in. when the plateau region reached the solidus, Fig. 11C.

Figure 11A shows the gradient conditions adjacent to the riser for a similar 4-in, thick casting. As a point 2 in. from the riser no measurable gradient was noted in the casting until it had cooled for 40 min. The maximum distance which the gradient extended into the casting during the solidification process was 4 in. Shrinkage was found 1 in. from the riser despite these thermal gradients. No reason for this anomaly was determined. It may be due to the relative mildness of the gradient near the riser or to internal tears formed when the cooling contraction of the casting is hindered by the then solid base of the riser.

Relation of Carbon Content to Soundness-The thermal gradients existing in 2-in. thick, 16-in. radius semicircular castings of 0.015, 0.21 and 0.34 per cent carbon are shown in Fig. 12. There was no essential difference, either thermally (except for the absolute

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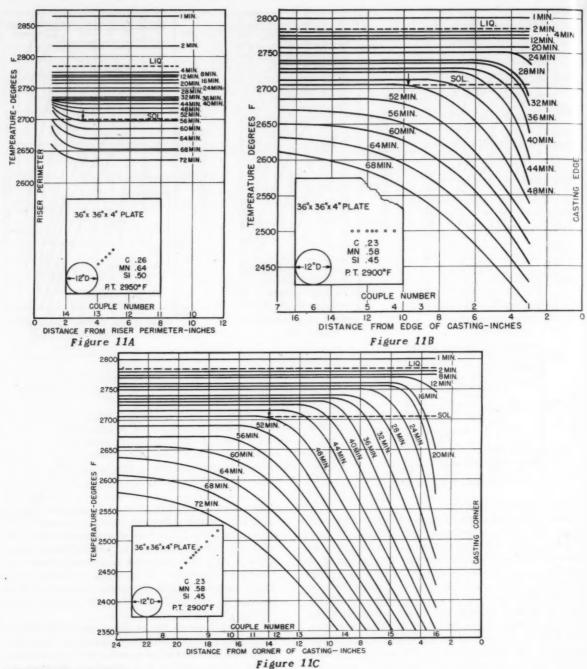
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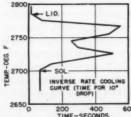
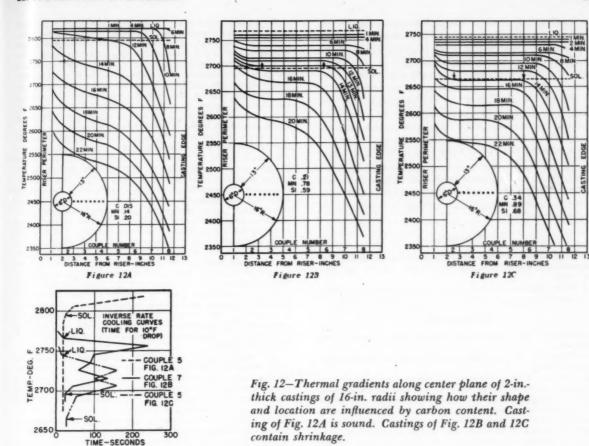


Fig. 11—Thermal gradients along center plane of 4-inthick castings at various intervals after pouring. Fig. 11A shows gradients resulting from heating effect or riser. Fig. 11B shows gradients resulting from chilling effect of edge. Fig. 11C shows gradients resulting from chilling effect of corner.

value of the temperature) or in the amount of shrinkage formed between the 0.21 and 0.34 per cent carbon steels (Fig. 12B and 12C). Both castings showed a

pronounced near-isothermal plateau. The low carbon steel casting, however, developed a pronounced gradient over the entire casting before the completion of



solidification and was accordingly completely sound. It should be noted that a 0.015 carbon steel, Fig. 12A, has a considerably shorter solidification range than 0.21 and 0.34 per cent carbon steels which may explain the increase. A high carbon steel apparently is more difficult to feed than one containing lower carbon. This trend is not as readily apparent in the castings of 12B and 12C, since the increase in carbon content from 0.21 to 0.34 per cent does not cause marked widening of the solidification range.

Conclusions

It was determined radiographically that plate castings of a width to thickness ratio of 3 to 1 or greater, containing 0.20 to 0.35 per cent carbon, can be made sound for a distance from the riser edge equal to about $4\frac{1}{2}$ times their thickness $(4\frac{1}{2}T)$. Of this distance, $2\frac{1}{2}T$ results from the chilling effect of the casting edge; the remaining 2T is made sound by the riser. These data are valid for risers having diameters equal to or greater than 3 times the casting thickness, and heights equal to $1\frac{1}{2}$ times the diameter. Increasing either the diameter or the volume of the riser beyond these limits has no beneficial effect in promoting casting soundness.

It should be possible to apply these data to large plates requiring several risers by locating each riser according to a system of triangulation such that each point on the casting is either within a distance of 2T from a riser or $2\frac{1}{2}$ T from a chill edge. By calculating

the surface area and volume of the segment which each riser feeds when located according to these relationships and then applying the formulae developed by Caine,¹ the minimum riser sizes which prevent both under-riser shrinkage and centerline-shrinkage can be determined.

Thermal analyses studies have shown that the soundness of plate castings of uniform thickness can be related to the development of three thermal zones during the solidification process: (1) The gradient zone determined by the chilling effect of the edges of the casting, (2) the gradient zone determined by the heating of the riser, and (3) a plateau of essentially uniform temperature between zones (1) and (2) which is not thermally influenced to a measurable degree by either the riser or the casting extremities.

As solidification progresses, the chill edge and riser gradient zones encroach upon the near-isothermal plateau. If the casting is sufficiently short, the near-isothermal plateau will be eliminated before the solidus is reached and the edge zone will join the riser zone to form a continuous gradient over the entire casting to assure progressive solidification, hence, soundness. If the expanse of the casting is enlarged, the near-isothermal plateau will not be completely eliminated by the time solidification is complete. If the length of the plateau at the solidus temperature exceeds approximately the casting thickness, shrinkage will be present in the casting.

The actual length of the steep gradient region at

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the casting edge when the solidus temperature is reached in the central plane of the casting determines the extent of sound metal at the edge regardless of the condition of the remainder of the casting. Paradoxically, the mild gradient region adjacent to the riser does not always insure soundness. No explanation can be offered for this anomaly, except perhaps in the relative mildness of this gradient or to the possibility that the "shrinkage cracks" are internal hot tears resulting from unequal contraction stresses.

It was found that a 0.015 per cent carbon, 2-in. plate casting could be made sound over a distance approximately 30 per cent greater than that indicated for the 0.20 to 0.35 per cent carbon steels. This would indicate that a short solidification range is conducive to

improved feeding.

References

1. J. B. Caine, "A Theoretical Approach to the Problem of Dimensioning Risers," Transactions, A.F.S., vol. 56, pp. 492-501 (1948).

2. W. H. Johnson and W. O. Baker, "Gating Systems for Metal Castings," Transactions, A.F.S., vol. 56, pp. 389–397 (1948).

DISCUSSION

Chairman: JOHN JUPPENLATZ, Lebanon Steel Foundry, Lebanon, Pa.

Co-Chairman: C. H. LORIG, Battelle Memorial Institute, Columbus, Ohio.

V. Paschkis (Written Discussion): 1 The authors are to be congratulated on an unusually clear written and important work. Tying in the phenomena of shrinkage with definite information on temperature gradients is important and should open up valuable avenues of research. Equally important is the finding that the location of the riser may be just as important as its size; the influence of edges and corner effects has been brought out clearly.

There are a few points which would stand clarification and

possibly additional information.

1. In the section on "Feeding of Circular Sections Representative of Wide Plates" it is stated that if the radii of plates increase above a certain critical value, radial shrinkage develops near the riser, whereas a certain zone near the rim remains sound. This seems to be in contradiction with the statement in the section on "Feeding of Rectangular Plate Castings Representative of Narrow Plates" where it is stated that the riser accounts for a zone of soundness which is given by a distance equal to two times the casting thickness. It should be expected that even in wide plates a zone of soundness would always exist in the immediate neighborhood of the riser. If this is not so, then necessarily there are plates where the influence of the riser decreases, because the demarcation between "narrow plates" and "wide plates" is not rigid.

2. In Fig. 9 to 12, time-temperature space distributions are shown. It would be desirable to have more such curves also for those castings which were not sound. It is conceivable that a critical gradient could thus be obtained below which unsound castings are to be expected. Calculation of the temperature gradients e.g. by means of the Heat and Mass Flow Analyzer would make possible a prediction of soundness of castings.

3. The authors point out in two cases deviations from their empirical rules. The one is the behavior of the $\frac{1}{2}$ -in. thick castings of semi-circular shape and the second the behavior of 4-in.

wide flat castings.

It would be desirable to have a complete thermal study of these conditions and possibly results for intermediate castings of say 34-in. thickness, semi-circular and 5-in. width of the flat type. Inasmuch as discontinuities in rules governing solidification should not be expected, additional information on these casting sizes should make it possible to explain the apparent deviation in the two cases mentioned and to form more inclusive rules.

MR. PELLINI: The apparent contradiction between the rule of 2T as a zone of soundness promoted by the riser and the extension zone is based on an assumption that risers will always promote a region soundness in the subject intersect-zone. This investigation has disclosed that this widely accepted concept is fallacious. It should be emphasized that the 2T rule applies only to completely sound castings. If the casting is large enough for shrinkage to form, the shrinkage will extend very close to the riser and the feeding range of the riser will then be negligible. Investigations are under way to determine the physical mechanism which produces this surprising behavior.

We share Dr. Paschkis' interest in the critical gradient condition below which unsoundness is to be expected. This is a question which should be resolved by consideration of the thermal gradient conditions critical for interdendritic feeding of the casting section at its solidification center point. The time-temperature-space distributions of Fig. 9 show that a condition of essentially zero gradient (within the accuracy of the thermal measurements) of as much as IT length may be tolerated without developing macroscopic shrinkage. This may be interpreted to mean that for practical purposes semi-infinite plates can be made sound only over a distance of 1T. Inasmuch as most of the theoretical analyses of solidification conducted by Dr. Paschkis and others have assumed infinite-section conditions it is important to recognize that such conditions are not compatible with soundness. The application of the Heat and Mass Flow Analyzer to studies of gradients requisite for soundness would therefore represent an extension to practical conditions having complex heat flow vectors. The data presented herewith for corner and edge gradients would possibly provide such an entry into this field of great practical significance to progressive solidification conditions.

The behavior of the 1/2-in. thick castings and of the narrow (4-in. wide) 2-in. thick castings should not be construed as representing discontinuities in solidification behavior. Dr. Paschkis is certainly correct in pointing out that on theoretical grounds such continuities are to be expected. The apparent greater chill edge and feeding distances obtained for the 1/2-in. plate are considered due to the minuteness of the shrinkage voids developed in this thin casting which precluded an accurate determination of the interdendritic feeding end point. It should be recalled that the sensitivity of the x-ray measurements was in the order of 11/2 per cent; with increased sensitivity it probably could be shown that the discontinuity does not exist. In the case of the 4-in. wide sections we have a condition in which two freezing edges are operative, hence considerably removed from simple plate conditions. This represents a condition having complex heat flow vectors for which the available information is inade-

quate for rigorous analysis.

CHARLES LOCKE:² I believe the authors used radiographic methods as their criteria of soundness. From the location of the temperature isotherm, which they call the knee, I would venture to say that the castings could not be considered sound. Right at the location where the isotherm is located, I think a sample under the microscope would show a great deal of interdendritic shrinkage. In order to have the plate completely sound, the temperature value at the solidus would have to be a point and not a line showing that the temperature has decreased from the edge towards the last liquid metal.

MR. PELLINI: I would like to ask Mr. Locke if the point of temperature change referred to is the knee or the length of the

isotherm

MR. LOCKE: The length of the isothermal.

MR. PELLINI: We, of course, agree with the ideas advanced by Mr. Locke. Soundness throughout this work should be interpreted as soundness to the limit of 1½ per cent radiographic sensitivity. Microscopic studies would probably show that what is called a riser 2-T effect is probably not much more than 1-T.

CHAIRMAN JUPPENLATZ: Have you considered sectioning any of

the plates for microradiography study?

MR. PELLINI: No, we had not. We are engaged in studies which could be classed as critical experiments in the sense of Mr. Juppenlatz's question. We would rather schedule such studies as future work specifically on the nature of interdendritic feeding in the last stages of solidification and carry out the investigations with this definite end point rather than as a related investigation. The problem is certainly worthy of such treatment.

¹ Technical Director, Heat and Mass Flow Analyzer Laboratory, Columbia University, New York.

² Supervisor, Foundry Research, Armour Research Foundation, Chicago.

H. F. TAYLOR: ^a Mr. Brinson made a study of steel castings of various shapes, with the risers directly above the castings. He found that risers feed about as far as is shown by this study. So it is not a surprise that the foundryman has to do something about it. He has found it necessary to change the gradients, pouring methods, and other things. I would like to suggest that one study the thermal size and shape of the system. For example, with two risers so close together, your thermal picture of the casting would show considerably different dimensions than a 1-in. slab made differently. A 1-in. slab with the riser on the gate is thermally an entirely different matter because of the heat concentrated around these risers.

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MR. PELLINI: The couples were placed through the cope side of the casting and extended down to the center plane so that the beads aligned along the center plane of the casting. The couples were enclosed in 1/32-in. wall thickness quartz tubing.

I agree generally with most of what Mr. Taylor has said except

in the matter of the solidification of metal on the thermocouple tubes, at least with the conditions of our experiments. We checked the accuracy of our couples in location by using companion couples placed in the molds so that the entire couple was a simple beam positioned across the mold section. Such procedures assure that a considerable length of the tubing is at temperature hence should eliminate conduction errors. We found that the absolutely reliable temperature readings obtained thusly checked with the projection type couples. The overall thermal gradients observed in the parts of the casting that have completely solidified, and also in the parts of the casting still at liquidus temperature corroborated the measurements made at any individual point. Moreover, the gradients show gradual and rational changes with time as solidification proceeds. There has been no indication whatsoever of a drop to the solidus temperature as soon as the liquid enters into the mold. As a matter of record, in the 2-in. plate 13 min was required for the last-tofreeze metal next to the critical couples to reach the solidus temperature. Certainly, it must be granted that a couple reading a hyper-solidus or liquidus temperature could not possibly be sheated by solid metal. An inspection of the temperatures shown by the couples during the entire course of solidification should provide evidence that Mr. Taylor's postulate is not tenable.

³ Associate Prof., Mechanical Met., Massachusetts Institute of Technology, Boston.

TECHNIQUE OF PRODUCING PRESSURE CAST ALUMINUM MATCHPLATES

By

Kurt A. Miericke*

Use of Plaster or gypsum cement composition as a molding medium into which nonferrous alloys are cast is an old art. But its application for the production of commercial castings was not brought to its high state of perfection until World War II. The method of pressure casting aluminum matchplates seems to have developed more rapidly than other branches of this art. Some of the reasons for this rapid development are:

1. Lack of newly trained molders and the ability of foundry training programs to train new molders, have made it necessary to simplify the molding operation so that untrained labor could do this work.

2. Tremendous increase of production during World War II without the aid of the skilled made the use of gated matchplates a must wherever possible.

3. A demand for greater precision and more uniform sand casting has been created by the competition of other casting processes such as permanent mold, plaster mold and pressure die casting.

4. An increase of production is possible by the use of gated matchplates as gated multiple cavity molds can be produced in less time than when loose ungated patterns are used to produce the cavity.

Making Pressure Cast Matchplates

Recent publications by Touchman, Bremer, Callahan and illustrative movies by Schleede and Miericke have done a great deal to clarify this process. But in most instances the most important steps on which the accuracy and soundness of a matchplate depend have been treated too lightly. A brief summary of the process of making pressure cast matchplates is essential before describing in detail some of the most critical steps in making a usable matchplate. The steps necessary in making a matchplate are as follows:

1. Making a follow board to produce a parting of the pattern. The follow board may be a piece of wood or gypsum cement of sufficient area to permit development of a parting line from the pattern to the flask joint, without too steep a gradient such as an irregular parting or a deep draw. 2. After a parting has been determined locating pins are placed to minimize mismatch between the cope and drag segments.

3. The specified number of matched cope and drag are made by casting a plaster composition slurry onto the pattern, the slurry being confined by a frame or cottle outlining the size of the sections.

4. The segments are trimmed and the sides notched to give better plaster adherence.

5. All cope sections are placed on a surface plate or plate glass surface, face down, in the manner they are to appear on the matchplate.

6. A flask frame is placed on the plate glass surface surrounding the sections. Weights are placed on each sections to minimize floating, and a plaster slurry is cast into the flask, binding the sections into a solid slab.

7. After the plaster composition has hardened the flask is released from the surface plate, inverted, and the corresponding drag sections are placed into position, and weights are placed on each section.

8. Another flask is placed on the first one and positioned with locating pins.

9. A separating or parting medium is applied to the surface which will receive the new slurry of plaster. The second flask is filled with a plaster slurry. After this slurry has hardened the two flasks are separated and the gates are cut. The individual mold locating pins can now be removed and the holes filled with a new plaster slurry.

10. The molds are now placed in the oven and burned out until they are dry.

11. After the molds are removed from the oven the matchplate thickness and contour outlined are placed in position, chills are placed, and the flask units are assembled by clamping and placing of the pressure chamber.

12. The aluminum alloy is melted, fluxed and cooled sufficiently to be near its solidification point, and then poured into the chamber.

13. The pressure cap is placed and secured and the air is applied, causing the aluminum alloy to take the shape of the cavity.

14. After sufficient cooling the assembly is broken

^{*} Baroid Sales Div., National Lead Co., Chicago.

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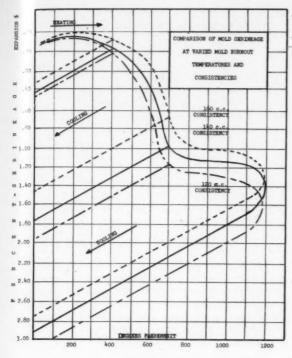


Fig. 1

open and the matchplate cleaned of excess plaster.

15. The gating system is removed and the matchplate is ready for the pattern shop and its final cleaning.

The basic principles involved in successful metal casting in plaster are simple, but successful applications of these principles into practice are not. Foundries embarking on this process should be prepared to work out details required to insure success, and should understand that this may be costly and slow. The two most important phases of this process on which success or failure depend are:

1. Method of preparing, casting and drying of the plaster casting medium.

2. Type of aluminum alloy used and its method of casting.

Mold Material

The material used to make the molds is a combination of gypsum plaster and a fibrous talc. The gypsum plaster when combined with proper proportions of water will hydrate and produce the strong accurate mold. The fibrous talc is used to add strength to the mold after burnout and to create greater permeability in the mold. The proportions generally used are 75 to 80 parts of gypsum plaster and 20 to 25 parts of fibrous talc (both by weight). This composition can be purchased ready mixed. Many shops who prepare their own compositions may also add other materials such as silica flour, marble dust, fly ash, and hydrated lime to produce other desirable conditions which they deem essential.

When preparing the plaster slurry for casting definite proportions of plaster compostion and water (both by weight) must be used. For every 100 lb of plaster used 120 to 180 lb of water must be added and mixed properly to produce a pourable slurry. The plaster composition should always be added to the weighed amount of water and let stand at least 3 to 4 min before starting to mix. This time interval will allow the plaster to become wet and eliminate lumping. Mixing may be done by manual stirring with a wood paddle, or for more consistent results, by mixing with mechanically operated propellers. With either method the slurry must be mixed until it starts to cream or thicken. Insufficient mixing will cause the slurry to settle causing variations of density in the mold.

It is of utmost importance to establish and adhere to a definite plaster to water ratio. Varying this ratio in producing the many mold segments and producing the two halves of the matchplate mold will produce uneven mold shrinkage during the mold burnout, and subsequent mismatch of the cope and drag halves of the matchplate. (See Fig. 1). It is also important to standardize on mixing procedures to minimize settling. Settling and variation of mold densities will also produce uneven mold shrinkage and may cause mold warpage.

Expansion of the Material

During the setting action or hydration of the plaster composition a definite expansion of the material takes place. The expansion is due to growth of the needle-like interlacing crystals which form when the plaster composition is hydrating. The definite amount of heat created by the chemical setting action of the plaster will also increase the thermal expansion of the plaster.

The expansion can be partially controlled by the addition of chemicals or by the use of hot water when mixing the slurry. Chemicals are rarely used as they may cause a defective casting surface.

Figure 2 illustrates the relation of hot and cold water to setting expansion. The graph also indicates the rise in temperature of the cold water slurry during setting. The hot water mix shows only a slight rise

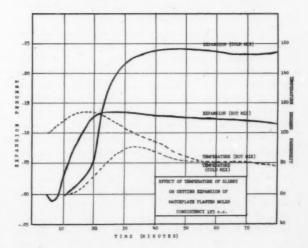


Fig. 2

in temperature and then steadily declines. The temperatures used are 160 F (71 C) and 40 F (4 C).

Before discussing the technique of mold burnout or calcination it is essential to understand the physical chemical principles of plaster.

The mechanics of setting calcined gypsum is such that approximately 18.6 lb of water are required to convert 100 lb of gypsum plaster (CaSO $_4$. $1/_2$ H $_2$ O) to set material (CaSO $_4$. 2H $_2$ O) in accordance with the reaction:

 $CaSO_4 \cdot \frac{1}{2}H_2O + \frac{1}{2}H_2O = CaSO_4 \cdot \frac{2}{2}H_2O$

Water in excess of this chemical requirement is necessary to produce a fluid slurry or plastic mass. In the case of calcined gypsum plaster consisting of small porous aggregates, a portion of the mixing water is taken into the individual particles by capillary action while the remainder of the water holds the particles apart. When the material sets this water is evaporated, resulting in air voids in the set dry material.

For every gypsum plaster mixed to a standard fluidity a definite quantity of water is necessary to fill the interstices between particles and provide sufficient lubrication for particle movement.

Drying the Mold

The time required for drying a mold varies with the mass and type of drying oven. Usually 18 to 20 hr are required to properly dry or calcine the mold. A dry mold does not mean one dry to the touch. Air-dried gypsum plaster contains approximately 18.6 per cent chemically combined water which must be driven off before metal can be cast in the mold. A recirculating type oven, gas or electric-fired, properly baffled so that no direct currents of air impinge upon the mold, is required for proper mold drying.

Uneven drying of the mold may cause the following:

1. Warping due to uneven mold shrinkage during burnout.

Segregation of the various mold segments and consequent mismatch due to the shifting of the segments in the mold.

3. Separation of the plaster composition from the metal flash.

4. Defective matchplates due to partial removal of moisture from parts of the mold.

The chemical composition of "set-up" gypsum plaster, air-dried, is CaSO₄. 2H₂O. A mold used for metal casting must be converted to CaSO₄ or the metal will not rest on the surface of the mold. This conversion of CaSO₄ . 2H₂0 to CaSO₄ is accomplished by subjecting the set-up plaster to a temperature of 400 F (204 C) for a period of time long enough to drive off all chemically combined water. This point can best be determined by imbedding a chromel-alumel thermocouple in the thickest part of the mold during the burnout. When this thermocouple registers a temperature above 375 F (191 C) the mold burnout is complete. The mold temperature will not go above 212 F (100 C) as long as free water remains in the plaster. The oven temperature must be lowered gradually to a maximum of 150 F (66 C) before the mold is removed. If the mold is removed from the oven while too hot the thermal shock, to which the

mold would be subjected, might ruin the mold.

A wet mold can be placed directly into a hot oven without danger of injury to the mold. A burned-out mold must not be allowed to stand in the open air for any length of time before introducing the metal since it will readily reabsorb moisture from the air and an unsatisfactory casting will result. In general, to maintain the precision required for matchplate work heat treatment is best done by maintaining baking temperatures between 350 F (177 C) and 400 F (204 C). Overbaking tends to cause the plaster to shrink away from the flask, with possible mismatch if the shrinkage is severe.

If temperatures over 450 F (232 C) are used for drying the molds excessive shrinkage will cause separation of the mold segments in the flask and may cause mismatch on the matchplate. (See Fig. 1).

Alloy Used for Matchplates

Because this molding material has relatively high insulative properties metal can be introduced into the mold at a temperature close to the solidification point of the alloy. Alloys used to make matchplates should have a relatively long solidification range and the following characteristics: (See Fig. 3).

1. High silicon content to minimize the solidification shrinkage, increase fluidity, and lower melting

temperature.

2. High copper content to improve castability and machinability.

3. Iron should be kept at a minimum to eliminate brittleness.

A typical aluminum alloy used extensively to produce satisfactory matchplates has the following analysis:

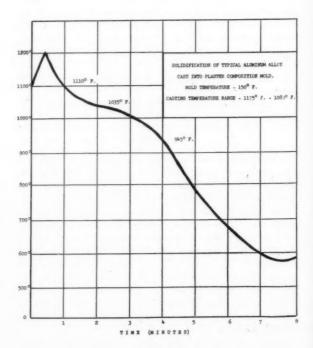


Fig. 3

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| Element | Per cent |
|----------------|--------------|
| Copper | 3.5-4.5 |
| Silicon | 7.5-8.5 |
| Zinc | 0.3 max |
| Iron | 0.5 max |
| Manganese | 0.3 max |
| Magnesium | 0.05 max |
| Other Elements | 0.2 max each |

The metal is melted and fluxed in the conventional manner and then chilled back until the metal is approaching solidification or in a mushy condition. This is the point at which the metal is quickly poured into the pouring basin and the cover quickly clamped in place. Pressure is applied to the casting chamber, allowing air to enter at the center of the cover until approximately 5 psi is reached (too great a pressure will cause casting to swell in the center). The mold is filled by the action of the air breaking the asbestos paper seal, allowing the metal to enter the down sprue.

The air pressure should be left on until the metal is solidified. The casting is taken out after cooling (2 or 3 hr or more) and plaster removed with wooden sticks and water. Chill stems are then cut off and gate removed. If the plate is made properly very little clean-up work will be required.

Summary

A list of precausions to be observed in making pressure cast aluminum matchplates are as follows:

1. Keep all parting surfaces as horizontal as possible. If parting surface is over 75 degrees with the horizontal, the plate will be too thin at that point. A vertical parting surface will make a hole in the plate corresponding to the position of the vertical parting surface.

2. Do not use lumpy plaster; the lumps are set up plaster and will cause the mix to set too fast and the dried molds will shrink too much.

3. Use proper ratios of plaster composition and water by weight to produce uniform mold segments.

4. Mix plaster composition sufficiently to minimize settling or segregation.

5. Use care to control mold burnout to minimize uneven and excessive mold shrinkage.

Chill all heavy section to promote uniform solidification.

7. Extra heavy section should be cored to lighten the matchplate and control solidification of the metal.

8. Metal should be cast as near the solidification point as possible to minimize metal shrinkage.

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A SUMMARY OF THE QUANTITATIVE EFFECTS OF SOME FACTORS ON THE ANNEALING OF WHITE CAST IRON

By

Richard Schneidewind*

BLACK-HEART MALLEABLE CAST IRONS have been produced in the United States since the early 1830's when Seth Boyden completed his experiments. The process as known today consists in producing a white iron casting which is annealed at a high temperature to decompose the primary carbides and then at temperatures below the critical to decompose the pearlitic carbides formed by transformation of austenite to pearlite.

Many factors influence the rate of conversion of white iron to fully annealed malleable iron. These may be divided into four categories as follows:

Composition of the iron.
 Melting and casting practice.

Cooling rate of the casting from the molten condition.

4. Annealing practice.

Each of these divisions can be further subdivided. At the present time the theory of graphitization has not been demonstrated satisfactorily and is but imperfectly known. Only a few of the possible factors affecting annealing rate have been studied quantitatively although qualitative information has been supplied for a larger number.

The variation in irons from heat to heat and plant to plant will be outlined briefly.

1. Composition of the Iron—Average American malleable iron has the following composition:

2.0-2.5 per cent carbon 1.0-1.5 per cent silicon 0.3-0.5 per cent manganese 0.1-0.15 per cent sulphur less than 0.15 per cent phosphorus

Foundrymen frequently encounter small amounts of chromium brought in with the scrap of the charge. Copper and boron have been added intentionally in some cases.

Melting and Casting Practice—The melting practice can vary from plant to plant with regard to the degree of superheat that is given to the melt. In addition, the length of time the metal is held at heat in the molten condition and the furnace atmosphere can also affect the degree of oxidation of the melt. This can influence grain size of the solidified casting, its annealing characteristics as well as its behavior in the mold as to gassiness, shrinkage, and surface cracking.

Other variations encountered are the pouring temperature and method of cooling: whether the casting is permitted to cool to blackness in the sand or is shaken out while white hot.

The sand and molding practice can also influence the annealing rate and the final properties.

Cooling Rate—Apart from the practice of shaking out while white hot or after complete cooling, major differences in cooling rate are due simply to the different section sizes of the castings themselves.

Annealing Practice—The difference in annealing involve the practice of either packing the white iron castings in a mixture of sand and iron oxide or of not packing; differences in the maximum soaking temperature, the rate of heating to the soaking temperature, and in some cases the practice of holding at a subcritical temperature prior to raising to the soaking temperature. The atmosphere surrounding the castings during annealing may affect annealing rates and can certainly influence the completeness of the anneal at the surface of the irons (picture framing).

It may safely be said that in view of the variables mentioned and the many possible ones not recognized, no computations can be made at the present time from which on a theoretical basis the annealing rate can be predicted. This paper will attempt to summarize the quantitative knowledge of those variables which are fairly well understood and to list some of the problems yet to be solved before a complete understanding of graphitization can be achieved. It is hoped that this information may prove of some guidance to workers in the field.

Summary of Factors Affecting Annealing Rate

The factors affecting annealing will be discussed in summary form. It must be clearly understood that in order properly to compare the effect of a certain variable such as soaking temperature all other factors R

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such as section size, superheat, etc., must be kept con-

Soaking Temperature—Identical castings if soaked at different temperatures above the critical will anneal at different rates. With increasing soaking temperatures the following behavior is noted:

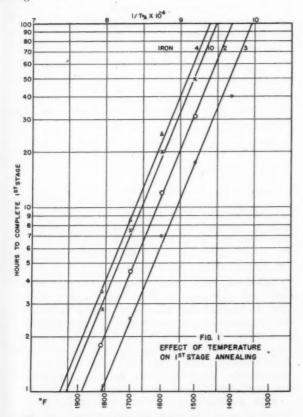
- a. Decreased time for first stage annealing.
- b. Temper carbon nodules more numerous and more closely spaced.
- c. Decreased time for second stage annealing.
- d. Slightly lower mechanical properties.

The time for completion of the decomposition of primary carbides varies with temperature is indicated1 in Table 1.

TABLE 1-ANNEALING TIME FOR VARIOUS IRONS, HR.

| | | | Tempeatur | e, °F | |
|------|------|------|-----------|-------|------|
| Iron | 1800 | 1700 | 1600 | 1500 | 1400 |
| 3 | 1.0 | 2.5 | 7.0 | 17.5 | 40.0 |
| 2 | 1.8 | 4.5 | 12.0 | 31.0 | _ |
| 10 | 2.8 | 7.5 | 20.0 | 50.0 | |
| 4 | 3.5 | 8.5 | 25.0 | >50.0 | _ |

These data plot on a straight line when time is plotted on a logarithmic scale and the temperature is plotted as the reciprocal $\left(\frac{1}{T}\right)$. This is indicated in Figure 1.



The equation of the lines is

$$\log \theta = \frac{10000}{T} + b$$

where θ = time in hours, T = temperature in degrees absolute, and b = a constant. The constant, b, includes all variables except time and temperature.

The most useful form of computation assumes that one casting has been annealed in the laboratory or represents past experience. If it is planned to anneal the second casting at another temperature, the annealing time can be computed.

$$\log \frac{\hat{\theta}_2}{\theta_1} = 10000 \left(\frac{1}{T_2} - \frac{1}{T_1} \right)$$

Example: Two identical castings. The first was annealed at 1700 F and 10 hr were required for the first stage.

$$\theta_1 = 10 \text{ and } T_1 = \frac{1700 + 460}{1.8} + 1200^{\circ} \text{ Absolute, } \frac{1}{T_1} = 8.33 \times 10^{-4}$$
The second casting is to be annealed at 1600 F

$$T_{a} = \frac{1600 + 460}{1.8} = 1144^{\circ} \text{ Absolute, } \frac{1}{T_{2}} = 8.75 \times 10^{-6}$$

$$\log \theta_{2} = \log 10 + (10000) (10^{-6}) (8.75 - 8.33)$$

$$= 1 + .42 = 1.42$$

$$\theta_{3} = 26.3 \text{ hr.}$$

More easily, if a line is drawn in Fig. 1 through 10 hr at 1700 F and parallel to the existing line, the time at 1600 may be read directly as 26.3 hr.

Temper Carbon Form

Second stage annealing below the critical varies with the closeness between temper carbon nodules. If soaking is conducted at higher temperatures, the nodules are closer and second stage annealing is

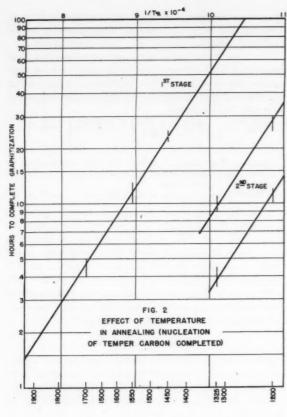
As the annealing temperature of first stage is increased the temper carbon assumes a more sprawly shape which decreases strength and particularly elongation. Above 1750 F this effect is very marked. Bardenheuer² and Massari³ showed that if white iron is soaked near its melting point, the temper carbon assumes a shape approaching that of gray iron.

The above equation with the slope of 10000 is the one generally accepted today. It may be deduced from data of Kikuta,4 White and Schneidewind1 and of Rehder.5 This value includes the two steps in graphitization, namely, (1) the formation of a nucleus or seed crystal of temper carbon and (2) the gradual solution of carbide, diffusion through austenite, and deposition of graphite upon the seed crystal.

An attempt was made to secure data where step (1) was already accomplished. A given iron was found to require 5.6 hr at 1700 F to complete graphitization. Three bars of the same iron were heated for 1 hr at 1700 and air cooled. Nucleation of graphite was thus completed. The samples were then reheated to complete first stage graphitization at 1700, 1550, and 1450 F respectively. The results were as follows:

In other words, the amount of graphitization incompleted after 1 hr at 1700 F required about 4.5, 11.5, and 23.5 hr at 1700, 1550, and 1450 F respectively.

Figure 2 shows these points plotted in the same manner as Fig. 1. The equation of this line is



$$\log \theta = \frac{6200}{T} + b$$

In other words, if the graphite or temper carbon has been started previously, the time required to complete graphitization is affected by the temperature less than where the iron is completely white at the outset.

This behavior is confirmed by second stage annealing below the critical temperature. Samples of two irons were annealed to completion at 1700 F, furnace cooled to 1400 F, and air quenched. The time required for second stage annealing to form 100 per cent ferrite was determined at 1325 F and at 1200 F. The results are as follows:

TIME FOR SECOND STAGE ANNEALING

| Iron | Hours at 1325 F | Hours at 1200 F |
|------|---------------------|--------------------|
| A | >3.5 <4.5 avg. 4.0 | >10 <12 avg. 11.0 |
| R | > 9 < 11 avg. 10.00 | >25 < 30 avg. 27.5 |

These values plotted in Fig. 2 fall on lines parallel to the one referring to the experiment carried on for the interrupted first stage annealing.

Two points from Kikuta's4 work give similar results although the slope of the line is 4500 rather than 6250. Nevertheless this would indicate that if graphite' nucleation is already complete, decomposition of carbides is affected differently than where nucleation and decomposition are considered together when the slope is 10000.

It has been shown⁶ that lower soaking temperatures lead to higher average physical properties. Within the range of 1650 to 1750 F the difference is slight. However, the irons from many of the older plants utilizing packing material in the furnace at soaking temperatures not over 1600 F have in many instances been somewhat superior to the same irons annealed by more modern practice.

Method of Heating to Soaking Temperature—The treatment of the white iron prior to its attaining full soaking temperature has considerable influence upon its annealing rate at that temperature.

It has been proposed that the iron be held for several hours at some subcritical temperature such as 800 F. Lorig⁷ and others have found that this will reduce the annealing time at the soaking temperature and produce a finer dispersion of temper carbon nodules. Bornstein⁸ indicated that holding at a higher temperature such as 1350 and 1550 F was helpful.

Boegehold9 showed that the rate of heating to the soaking temperature was most significant. A slow heating resulted in faster annealing at the maximum temperature and a finer dispersion of temper carbon.

The results of Schneidewind and Reese¹⁰ and a recomputation of Boegehold's data indicate that for a given iron heated to the soaking temperature at two different rates, the times of annealing may be ex-

$$\log \frac{\theta_2}{\theta_1} = -30 \left(\frac{1}{R_2} - \frac{1}{R_1} \right)$$

 $\log\frac{\theta_2}{\theta_1}=-30\bigg(\frac{1}{R_2}-\frac{1}{R_1}\bigg)$ where θ_1 and θ_2 are time in hours for the two bars and R₁ and R₂ are the rates of heating in degrees Fahrenheit per hour. The range of temperature in which heating rate is significant is from the critical temperature to the soaking temperature.

Example: A tensile bar is placed in a furnace at 1700 F. The bar is at temperature in 30 min. θ_1 is found to be 10 hr. 1700 - 70

$$R_1 = \frac{1700 - 70}{E} = 3260 \text{ F/hr.}$$

The second bar is placed in a furnace and brought to heat slowly. The time from 1400 F to 1700 F is 4 hr. Fine θ_3 at

$$R_2 = \frac{1700 - 1400}{4} = 75^{\circ} F/hr$$

$$\log \frac{\theta_2}{10} = -30 \left(\frac{1}{75} - \frac{1}{3260}\right)$$

$$\log \theta_3 = 1 - (30) (.0133 - .0003) = 1 - .39 = .61$$

 $\theta_3 = 4.17$ hr at 1700 F after heating to 1700 F slowly. Figure 3 shows the relationship between the rate of heating and the time at temperature. To solve the above problem a parallel line can be drawn through the point: 10 hr and 3260 F/hr; at 75 F/hr the time can be read off as 4.17 hr.

HOLIBS TO COMPLETE IST STARS

Superheat—Superheating means heating the molten metal above the melting point. Some degree of superheat is essential that the metal can be transported and poured and will completely fill the mold before solidifying. It has been found that higher temperatures than this tend to reduce the time for subsequent graphitization. Superheat is independent of pouring temperature, the latter affects the rate of cooling in the molds, while superheat does not.

It is possible that superheat acts similarly to increased time of holding in the molten condition. Both of these practices tend to reduce the number of graphite particles which exist in the melt from the charge. The particles are dissolved or oxidized. This is shown

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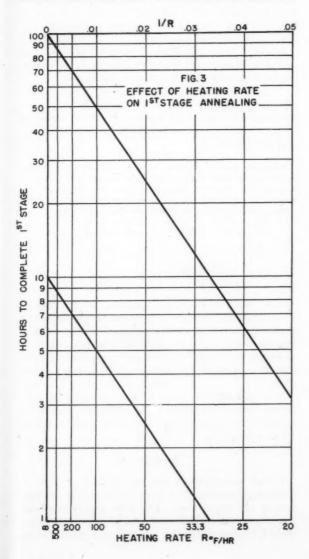
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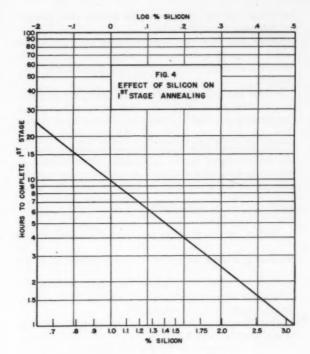
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i. reby the fact that test sprues become whiter as the charge is held at heat although the chemical composition is changing only slightly. If 2800 F is considered a normal temperature, each 100 F of additional superheat may reduce the annealing time as much as 10 or 15 per cent.6 It has been found, however, that fuel and refractory costs make undue superheat too expensive.

Deoxidation—Variations in the nature of the charge, the type of furnace used, the atmosphere above the melt, superheat, and other melting conditions can cause differences in the degree of oxidation of the melt. This factor has been studied quantitatively for only a few years. Heine11 has done interesting work in this field studying the influence of deoxidation. His findings indicate that up to a point, deoxidation increases the graphitization rate. Graphitization rate is decreased if the amount of deoxidizer exceeds a critical amount.

Silicon Content—It has long been recognized that the time for annealing white iron decreases as the silicon content increases. The maximum silicon con-





tent is limited practically by the ability to secure a white casting. Boegehold12 was able to exceed normal values by the ladle addition of tellurium; the process is not used commercially. Irons may contain high quantities of silicon but if treated with magnesium under the proper circumstances, white irons result. Annealing is relatively rapid, but the product is a true nodular iron and not a typical malleable iron.

Rehder⁵ replotted the data of Kikuta⁴ and along with his own data showed that the annealing time varied with the silicon in log-log relationship. A middle value taken from Rehder's work would indicate that

$$\log \theta = -2 \log Si + b$$

where $\theta =$ time in hours, Si = per cent of silicon, and b = a constant depending upon all variables except silicon. The data of Schneidewind and White1 are in agreement. Figure 4 shows this relationship graphically.

To use this relationship the following example is illustrative.

A tensile bar of an iron containing 1.0 per cent silicon is annealed in a laboratory furnace at 1700 in 10 hr. Hence, $\theta_1 = 10$ and $Si_1 = 1$. A second bar contains 1.5 per cent silicon and was made under identical conditions. Compute θ_2 for this

$$\log \frac{\theta_3}{10} = -2 \log \frac{Si_3}{Si_1}$$

$$\log \theta_3 = 1 - 2 \log \frac{1.5}{1} = 1 - .352 = .648$$

$$\theta_4 = 4.45 \text{ hr.}$$

 $\theta_{2} = 4.45 \text{ hr.}$

Or, by drawing a parallel line on Figure 4 through 10 hr at 1 per cent Si, at 1.5 per cent Si the time of 4.45 hr can be read

Carbon Content-The quantitative influence of carbon on annealing time is not known. Kikuta4 and Schneidewind and White1 indicated that, everything else equal, increasing the carbon increases the anneal-

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ing time. Rehder⁵ indicated that carbon may change the silicon-time relationship.

There are, however, too few data of irons made under the same conditions in which the carbon content varies significantly to draw any quantitative conclusions.

Section Size—Tests on step bars where it is obvious that all conditions of the iron are identical except for the cooling rates of the individual steps have shown that annealing time increases with section sizes.

The section size is a measure of the cooling rate of the molten metal in the sand mold. One method of reporting on a logical basis is to compute the ratio of volume of metal to area in contact with the sand.

V cubic inches of volume

This ratio, —, is computed as—

A square inches of surface

= inches. Many foundrymen may find difficulty in

imagining a ratio; hence, if $\frac{V}{A}$ is multiplied by 4, the

result is the diameter of a long bar whose cooling rate is the same as that of the original casting. This equivalent diameter may be written *De*.

The effect of cooling rate on time of first stage annealing can be shown¹³ to be

 $\log \theta = 1.2 \log De + b$ where $\theta = \text{time}$ in hours, De = equivalent diameter in inches, and b = a constant depending upon all variables except time and section.

Figure 5 presents this relationship graphically.

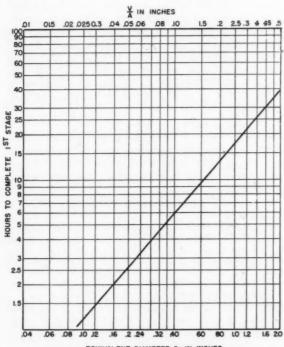


FIG. 5 EFFECT OF SECTION SIZE ON 18T STAGE ANNEALING

In order to use this relationship, it is necessary that the annealing time for one casting be known. The annealing time of another casting from the same heat may be computed. **Example:** A tensile bar casting, diameter = 5% in. can be annealed at 1700 F in 10 hr. Hence, $D_1 = 0.625$ in. and $\theta_1 = 10$ hr. Another casting from the same heat has a diameter of 1.25 in. (D_2) . Compute annealing time at 1700 F.

$$\log \frac{\theta_2}{\theta_1} = 1.2 \log \frac{D_2}{D_1}$$

$$\log \frac{\theta_2}{10} = 1.2 \log \frac{1.25}{0.625} = 1.2 \log 2 = (1.2) (.301)$$

$$\log \theta_2 = \log 10 + .361 = 1.361$$

$$\theta_2 = 23 \text{ hr.}$$

An easier way is to draw a parallel line in Fig. 3 which passes through 10 hr where De = 0.625 in. The time for 1.25 in. can be read off as 23 hr.

Effect of Various Elements—Quantitative data are lacking for most of the elements which may be added or be present in white iron.

Manganese is a carbide stabilizer; the effect of sulphur is similar. Rehder¹⁴ showed that minimum annealing time in second stage was secured if the amount of manganese was that required theoretically to produce MnS with all of the sulphur plus an excess of 0.16 per cent. At this composition the carbide stabilizing influence is lowest.

Recomputation of Rehder's data¹⁵ indicates that time for second stage graphitization at a given temperature varies with manganese as follows:

$$\log \theta = 2.0 \text{ Mn}_x + \text{b}$$
 where $\theta = \text{time}$ in hours, $\text{Mn}_x = \text{manganese}$ in excess over the combined manganese or in excess of (1.72) (%S), b = a constant including all factors except manganese.

Example: An iron contains 0.1 S and 0.4 Mn; second stage annealing has been found to be 15 hr at a certain subcritical temperature. Another iron made from the same melt but containing additional manganese contains 0.6 Mn. Find the time for second stage annealing at the same temperature.

The data for first stage annealing would indicate that manganese is about $\frac{1}{10}$ as effective in retarding graphitization than in second stage. More work is needed in this field because in nodular irons made by annealing from the white state, manganese is very critical in view of the exceedingly low sulphur contents.

The quantitative data on the influence of chromium, boron, and copper are insufficient to make any quantitative computations regarding their influence on annealing rates.

Summary

It has been pointed out that it is impossible to compute annealing rates from the chemical composition or any other factor. The exact influence of only a few factors is understood at the present time. Nevertheless, if careful data are secured on a given casting, DN

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approximate predictions may be made for other castings under similar conditions.

For example, a tensile bar 5/8-in. diam contains 2.3 C, 1.3 Si, 0.1 S, and 0.4 Mn. It is treated in a laboratory furnace already at 1700 F so that it comes to heat in 1/2 hr and requires 10 hr at 1700 F to complete first stage.

Another casting made at the same foundry using the same raw materials, superheat, sand molds, etc., contains 2.3 C, 1.0 Si, 0.1 S, and 0.5 Mn.

This casting is 1 in. square and 10 in. long. It is heated to 1750 F at such a rate that the temperature is raised from 1400 to 1750 F in 4 hr. Compute annealing time.

$$\log \frac{\theta_{s}}{\theta_{1}} = 10000 \left(\frac{1}{T_{s}} - \frac{1}{T_{1}}\right) - 30 \left(\frac{1}{R_{s}} - \frac{1}{R_{1}}\right) + 1.2 \log \frac{De_{s}}{De_{s}}$$

$$-2 \log \frac{Si_{s}}{Si_{s}} + 0.2 \left(Mn_{xs} - Mn_{xs}\right)$$

$$T_{1} = 1700 \text{ F} = 926 \text{ C} = 1199 \text{ A}$$

$$T_{2} = 1750 \text{ F} = 954 \text{ C} = 1227 \text{ A}$$

$$R_{1} = \frac{1700 - 70}{.5} = 3260 \text{ F/hr}$$

$$R_{2} = \frac{1750 - 1400}{4} = 87.5 \text{ F/hr}$$

$$De_{1} = 0.625 \text{ in.}$$

$$To compute De_{s}: \text{ Volume} = 10 \text{ cu in.}$$

$$Area = 40 + 2 = 42 \text{ sq in.}$$

$$\frac{V}{A} = \frac{10}{42} = .238 \text{ in.}$$

$$De_{2} = 4 - .952 \text{ in.}$$

$$Si_{1} = 1.3\%$$

$$Si_{2} = 1.0\%$$

$$Mn_{xs} = .4 - .332 = 0.068\%$$

$$Mn_{xs} = .6 - .332 = 0.268\%$$

$$\log \frac{\theta_{2}}{15} = 10000 \left(\frac{1}{1227} - \frac{1}{1199}\right) - 30 \left(\frac{1}{87.5} - \frac{1}{3260}\right)$$

$$+ 1.2 \log \frac{.952}{.625} - 2 \log \frac{1.0}{.} + .2 (.268 - .068) = -.180 - .333$$

$$\log \theta_{2} = 1.1760 - 0.0268 = 1.1492$$

$$\theta_{2} = 14.1 \text{ hr at } 1750 \text{ F.}$$

In any computations such as these all factors except those in the expression must be kept constant. If used with this limitation in mind it is felt that the empirical formulae proposed will be of assistance in predicting annealing rates for irons to be processed under a new set of conditions.

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DISCUSSION

Chairman: H. D. Hunt, Auto Specialties Manufacturing Co., St. Joseph, Mich.

Co-Chairman: F. P. WURSCHER, Chicago Railway Equipment Co., Marion, Ind.

MILTON TILLEY:1 You undoubtedly are familiar with the increased graphitization rate obtained by heating hard iron above the critical temperature followed by quenching. I wonder if this 6000 figure you have against 10000 might have been affected by that quench you gave the sample from the first stage of anneal.

Mr. Schneidewind: By heating above the critical, first, you are starting to nucleate temper carbon. Only a small amount is

accomplished there.

At the temperature to which you heat, the matrix austenite is saturated at that temperature. When you quench you get a martensite, which will have much more carbon in it than theoretical eutectoid saturation.

We start heating again, and we have a very supersaturated austenite which I believe will precipitate many more nuclei of temper carbon than you would get by heating normally the first time. I do not know what the mathematical arrangement is.

¹ Metallurgist, National Malleable & Steel Castings Co., Cleveland.

METAL SPECIFICATIONS FOR THE BRASS AND BRONZE FOUNDRY

By James G. Dick*

THE WRITER would like to remark at the outset that the metal specifications, to which he has reference in this paper, cover only those for composition ingot metal. Where purchases of virgin metals are concerned the problem of covering such purchases by specifications can be solved by having recourse to the use of specifications already compiled and listed by the producers of virgin metals, or by reputable standards bodies. Where binary alloys, such as phosphor-copper and nickel-copper, are involved these purchases are, in the writer's company, covered by the use of a specifications system that is now in the final stages of reconstruction. No discussion of these specifications will, therefore, be included in this paper.

There are only two courses of action open to the foundry which must manufacture castings in a wide range of alloy types. The alloys required can, for one thing, be compounded by the foundry itself from virgin metals, from scrap materials plus virgin metal additions, or from other alloys plus virgin metal additions. Many disadvantages are inherently a part of this method of obtaining the alloy types required. These include: the introduction of a manufacturing process entirely apart from the essential function of the foundry, which is the production of castings; a basic lack of economy; the often encountered difficulty in obtaining a source, or sources, of homogeneous scrap materials at reasonable cost; the necessity of manufacturing small heat weights for each alloy type; finally the ever-present doubt as to the ultimate homogeneity of the final product.

The other alternative is for the foundry to purchase, from a reliable ingot manufacturer, the alloy types required in the form of composition ingot metal. The disadvantages outlined for the previous method of supply are largely eliminated in this instance. This is particularly so when purchases of this nature are governed by the application of an adequate metal specifications and testing system. The writer has always favored the purchase of ingot metal by the foundry, for the simple reason that, given a reliable source of

ingot metal, this together with good foundry practices is sufficient to assure the production of good castings and the removal of any doubt as to the basic quality of the metal being used.

Prior to the last war the organization with which the writer is associated used a system of ingot metal specifications which covered only those alloys that were cast in comparatively large quantities. These specifications were, moreover, specific as to individual composition ranges only from the point of view of the principal alloying elements. Insofar as the allowable impurities were concerned for each alloy type, these were either omitted or were covered by a blanket clause such as: "Not more than 1.0 per cent total impurities." On the face of the matter it would appear that this situation alone would have justified a reconstruction of the entire metal specifications system. The impending reconstruction was, however, hastened along by a further development.

During the war years, and subsequent to them, we were called upon to produce castings of all types in a wide range of alloys for industry in general. In the initial stages of production this frequently entailed the handling of alloys with which we had had but little previous experience. In view of the necessity of devoting all of our attention to the development of foundry practices that would permit the production of good castings, it became essential that the ingot being purchased to provide these alloys be such that no doubt could exist as to alloy composition and quality. This required that specifications be drawn up to cover each of the alloys in question, and it was therefore decided to reconstruct the entire metal specifications system from the ground up. This reconstruction was planned so as to embrace eventually both composition ingot metal and binary alloys, in fact all metal purchases other than those covering virgin metals.

After some expenditure of time and effort, during which we were aided by the suggestions and criticisms of the Canadian ingot manufacturers, a fairly comprehensive specifications system for ingot metal was prepared. It was the writer's good fortune to be associated with the work of reconstruction from its inception,

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and it is his intention in this paper to outline the methods and ideas involved in the preparation of these specifications.

General Considerations

Before any steps could be taken in the direction of arranging the chemical composition and mechanical properties of the individual specifications, consideration had to be given to such matters as applied to ingot metal in general, regardless of the alloy involved. A goodly number of points that had application in this general sense were found, even as a result of the briefest of surveys of the situation. The following lists those finally considered to be of primary importance:—

- 1. Size of the melt per ingot heat.
- 2. General characteristics of the ingot.
- Method of marking the ingot for the purposes of identification.
- 4. Method of loading the ingot for shipment.
- Methods to be used in sampling, testing and approving any heat of ingot.

The writer would like to deal briefly with each of these points, outlining the ultimate conclusions as these were drawn up and incorporated in the specifications system.

1. Size of melt for any heat of ingot metal is a matter of some importance to the foundry. When an order is placed for, say, 120,000 lb of ingot metal to a single alloy specification it would complicate matters considerably to have this order filled by 24 separate heats of ingot, each of 5,000 lb. Two important factors are involved here. There will be, first of all, an extension of the time required to fill the order, if this is done by supplying a large number of small heats rather than two or three large heats. Secondly, in order to avoid any doubt as to the alloy composition, it would be necessary for the foundry to conduct 24 separate sets of tests to determine the individual ability of each heat to meet the specification requirements. This again is a time-consuming procedure and one that would result in an extension of the time elapsing between the preparation of the ingot and its approval for use in the foundry.

In view of these factors it was decided to include in the system a clause embodying our conclusions in this connection. After a survey of the capacities of the Canadian ingot manufacturers the following wording was used in drawing up the clause in question:—

Clause 1

The ingot manufacturer shall endeavor to fill each order for any one alloy type by supplying a single uniform melt of the metal involved. Where the requirements for any one alloy type exceed 40,000 pounds this order shall be filled by supplying the required weight in as few uniform melts as possible.

2. Some considerable thought was given to the question of what should be specified in connection with the general characteristics of the ingot to be supplied to us. The main points to be covered, it appeared, were the dimensions of the ingot, the condition of the ingot tops, the appearance of the fractures and the homogeneity of the composition of the ingot comprising any single heat.

Since we had no particular preference as to the exact shape or type of ingot, having been well satisfied

with the type of ingot supplied to us by the Canadian manufacturers in the past, it was decided to outline the required ingot dimensions in a general way only. Approximate dimensions were therefore drawn up, after considering the ingots obtainable from different sources, and no attempt was made to hold the ingot manufacturers to supplying any other than these approximate dimensions. One important factor was incorporated in the clause covering the shape of the ingot and this was a statement to the effect that ingot having a deeply notched bottom was not to be supplied except in the rare instance where this was unavoidable. Our desire to avoid this type of ingot was based, not on the understanding that notched ingot was in any way inferior to the notch-free variety but rather on the fact that our method of fracture testing and of sampling for chemical analysis required a notch-free ingot.

The writer has no wish to enter into any controversy concerning the relative merits of smooth top and rough top ingot. The metal specifications used now by his organization state that all ingot is to be supplied with smooth tops. This type of ingot is stipulated because it provides an excellent area upon which to stamp the ingot clearly for identification purposes.

It is the writer's firm opinion that, of any single test that can be performed on ingot metal, the examination of the properly fractured ingot is by far the most important and the most revealing. The fracture test clause, as it appears in our present specifications, was transferred unchanged from the previous system.

The uniformity of the composition of the ingot metal comprising any one heat, regardless of its time of origin from the furnace melt, is an important point for the foundry to consider. Ingot samples selected from the initial pour, the middle pour and the final pour at the ingot manufacturer's plant should not show, upon chemical analysis, any variation in the composition beyond the limits prescribed by the particular specification involved. That special attention would have to be paid to this factor when reconstructing the specifications system was apparent from the beginning, since much of the ingot metal used by us was in the nature of high-lead bronzes.

The clauses covering the items listed in the foregoing were worded in the following manner:—

Clause 2

All ingot shall be supplied with the following approximate dimensions.

| Top length | 10 is |
|---------------|-------|
| Top width | 3.5 ' |
| Bottom length | 9 ' |
| Bottom width | 2.5 ' |
| Thickness | 2 ' |

All ingot shall be supplied with rounded ends. Unless under exceptional circumstances and where permission has previously been obtained in writing, no ingot having a notched bottom shall be acceptable.

Clause 3.

All ingot shall be smooth top.

Clause 4.

All ingot shall, upon being fractured in the manner described elsewhere in these specifications, display a close-grained structure and a homogeneous color throughout the fractured crosssections. There shall be no indications of any porosity, holes or discolorations.

Clause 5.

All ingot of any one heat shall be of a uniform composition and shall not, upon chemical analysis, show a value for any component outside of the limits of the range given for that component in the applicable specification.

3. It is always an advantage for the foundry, in the event of any difficulty being experienced in the use of ingot metal in casting production, to be able to pick out the individual heat number of the ingot causing the trouble. The doubtful ingot heat can then be segregated and removed from use. To do this in the easiest manner requires that the various shipments of ingot be stored in the foundry not only according to the alloy type but also according to heat number.

The methods of identification proposed and finally included in the specifications system covered, therefore, the type of metal, the heat number and the ingot manufacturer's name or mark. Where the heat number was concerned we asked that these should be consecutive from the beginning to the end of each year, regardless of the alloy type. In this way we avoided any repetition of a heat number in any one year.

In order to facilitate alloy type identification we devised a color code system, as well as a code letter system. In arranging the details of the color code system a basic color was assigned, wherever possible, to each general alloy group. Alloys containing silicon were, for example, assigned black as the basic color, while those containing aluminum were allotted brown as the group color. The clause covering ingot metal identification is given below;—

Clause 6.

In any particular heat of ingot each ingot shall be plainly marked in a manner enabling us to identify the manufacturer of the ingot. Each ingot shall further be marked plainly with a steel stamp, in letters and figures not less than ½ inch in height, in a manner enabling us to identify the type of metal and the number of the heat from which it was derived. The heat numbers used shall be in consecutive order from the beginning to the end of each year, this regardless of the alloy type involved. Each ingot of any particular composition shall also be marked clearly with a colored paint, or colored paints, in addition to being stamped with the proper code letter. The code letter system and the color code system shall be as outlined in Table 1 below.

TABLE 1

| Alloy Type | Color Code | Code Letter |
|-------------------------|----------------------|-------------|
| | | |
| "A" Metal | Red | A |
| "B" Metal | Yellow | В |
| 15% Lead Metal | Green | E |
| 22% Lead Metal | Green & Red | J |
| 80-10-10 Metal | Green & White | Z |
| 8-15 Metal | Green, White & Red | R |
| Gun Metal | White | G |
| 90-10 Metal . | White & Red | Y |
| Silicon Bronze Alloy | Black | H |
| PMG No. 1 Metal | Black & Red | T |
| PMG No. 4 Metal | Black & Yellow | U |
| Hyhete Metal | Brown & Black | Q |
| Aluminum Silicon Bronze | Brown, Black & White | W |
| Aluminum Iron Bronze 1 | Brown | L |
| Aluminum Iron Bronze 2 | Brown & Dark Blue | LP |
| 75000 Manganese Bronze | Brown & Yellow | S |
| 100000 Manganese Bronze | Brown & Red | P |
| Iron-Manganese Bronze | Brown, Red & Yellow | D |
| Phosphor Bronze | Light Blue | F |
| 2-B-8 Metal | Light Blue & Red | x |

4. Little need be said here regarding the method that was specified in connection with the loading of the ingot for shipment to us. We stressed only the point that each different type of metal and each different heat number had to be kept separate in the shipment. The clause involved here reads as follows:

Clause 7.

In the case of a shipment where more than one heat of ingot and/or more than one type of metal is to be shipped together, each heat and/or each type of metal shall be piled separately and shall be blocked off in the car, as well as being marked in accordance with Clause 6.

5. A sure way for the ingot manufacturer to lose money and for the foundry to lose valuable time, is for an entire heat shipment of ingot to be shipped into the foundry and subsequently rejected and returned on the basis of failure to meet the specification requirements. As a protection for both the ingot manufacturers and ourselves it was decided that no heat of ingot metal was to be shipped to us until an approval for shipment had been issued, this approval to be based on the results of preliminary tests conducted in our own laboratories. This was to constitute an approval for shipment only, however, the final and ultimate approval to be issued on the basis of the results of tests performed in our laboratories on samples procured after shipment to us of the entire heat. The clauses that embody our thought in this connection are given below:-

Clause 8

Each heat of ingot made by the ingot manufacturer must be approved by us, as to both chemical and mechanical characteristics, before shipment to us of that heat. No heat of ingot shall be shipped to us without this approval of shipment. Such an approval shall be based on the results of our tests as conducted by our laboratories. The ingots necessary for such a group of tests shall be sent in by the ingot manufacturer and shall be selected by him from the entire group of ingots representing the heat to be tested. Such a selection shall be made in a random manner, one ingot being selected for each 5,000 pounds in the heat. Regardless of the weight of material comprising the complete heat never less than five ingots shall be selected. These sample ingots shall not be drilled or fractured by the ingot manufacturer.

Clause 9.

The final approval of any heat of ingot metal shall be based on the results of our tests as performed on ingot samples procured after shipment of the heat to the plant involved.

The writer would like now to outline the method prescribed in the specifications system for the sampling and testing of ingot metal shipments. This will be done by giving a direct extract from our specifications system. Only one matter requires additional comment here and this concerns the Keel Block Mold used in connection with the preparation of the test bars required for the mechanical tests. This type of test bar mold has been used in our organization for some period of time and was introduced into service there by Harold J. Roast. It has been found to give entirely satisfactory results with ordinary alloys, phosphor bronze alloys and high shrinkage alloys.

DETAILED SPECIFICATIONS FOR THE SAMPLING AND TESTING OF BRASS AND BRONZE INGOT METAL

 Each heat of ingot of any particular composition shall be sampled and tested according to the instructions following. od

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- 2. Ingot shipments in excess of 25,000 pounds shall be sampled by selecting at random at least one ingot for each 5,000 pounds in the heat shipment. Care should be taken that, while making the random selection, the individual ingots are selected from different stages of unloading the car or at different points of the piled ingots.
- Ingot shipments of less than 25,000 pounds should be sampled by a method similar to (2), but the selection should be such that five ingots selected at random will be the minimum sample taken.
- When the random selection has been made each ingot shall then be cross-sectioned by fracturing it exactly in half along an axis at right angles to the longitudinal.
- The fractured cross-section shall then be examined carefully and must expose a close-grained structure and a homogeneous color throughout.
 There shall be no indications of any porosity, holes or discolorations.
- 6. After examination of the cross-sections of each ingot, these cross-sections shall then be drilled in five places, the location of the drill holes being one in each corner of the ingot section and the fifth in the center. Each hole shall be drilled to a uniform depth of 1/2 inch and the drill bit used shall be 1/2 inch in diameter. The manner of operation of the drill shall be such as to yield chips of as small dimensions as is practically possible. Long spiral cuttings must be avoided. The cuttings or drillings procured from each ingot shall be thoroughly mixed together and a sample of a size suitable for laboratory use shall be obtained by quartering down the bulk sample. In no case shall water or cutting oils be used during the drilling of the ingots and, prior to any laboratory analysis, the entire sample

shall be worked over with a magnet in order to remove such iron as may have been introduced during the drilling operation. All bulk and laboratory samples must be kept for a three month period.

7. The fractured and drilled ingots are then to be used for the purpose of making up the test keel block from which the mechanical properties of the ingot under test shall be determined.

 Take a clean annealed plumbago crucible of adequate capacity. Put it in a suitable crucible furnace and bring it to a bright red heat.

Place the ingots in the crucible, putting them in with tongs and fitting them as closely together as possible.

10. Cover the crucible with a lid formed from the bottom of an old crucible. This lid must be carefully cleaned to ensure that there is no metal left adhering to it.

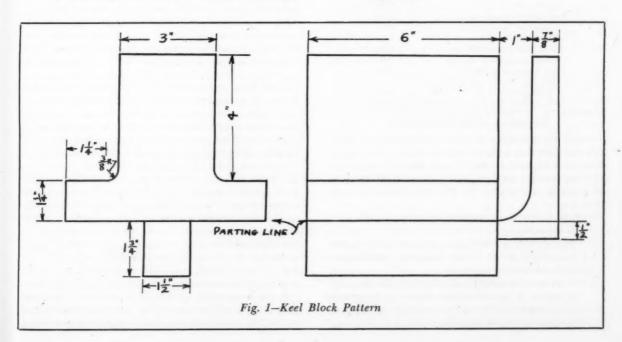
 Do not have any ingot protruding past the lid of the crucible. The lid must fit onto the crucible at all times

12. Force the firing as much as possible in order to melt the metal as quickly as possible. Be sure to maintain slightly oxidizing furnace conditions during the entire melting cycle.

13. As soon as the metal has melted bring it to a temperature of about 100 F to 150 F over the required pouring temperature for the type of metal involved.

14. Immediately after the required temperature has been reached remove the crucible from the furnace, transfer it to a skimming plate near the pyrometer, stir well in both directions, skim and stir again in both directions.

15. All skimmers and stirrers must be thoroughly cleaned and must contain no foreign materials of a nature such as to contaminate the alloy being tested.



- 16. Take the temperature of the metal with an accurate pyrometer and when it has reached the optimum pouring temperature for the type of metal involved, pour the melt into the unblackened standard dry sand mold of the keel block pattern. A sketch of this keel block is shown in Fig. 1.
- 17. Remove the keel block from the mold after the required period. This will be three hours in most cases. Allow it to cool in air and then stamp both ends of all three bars with markings identifying the maker of the ingot, the type of metal and the ingot maker's heat number. Stamp the feeding block in the same manner.
- Sand blast or otherwise clean and cut off the gate.
- 19. Saw the keel block bottom bar half way through in a longitudinal direction, cutting close to the feeding block. Break off this bar on the breaking machine with a wedge. The fracture should be of a close-grained structure and a homogeneous color. Saw off the two side bars, keeping one in reserve and sending the other to the laboratories for the required tests.
- The following lists the optimum pouring temperatures for each of the alloys listed in these specifications. These temperatures are for the Keel Block test bar mold.

| 5 | Alloy | ouring Temperature, ° |
|---|-------------------------------|-----------------------|
| | 'A" Metal | 1950 |
| | "B" Metal | 1900 |
| | 15% Lead Metal | 1900 |
| | 22% Lead Metal | 1900 |
| | 80-10-10 Metal | 1900 |
| | 8-15 Metal | 1900 |
| | Gun Metal | 1900 |
| | 90-10 Metal | 1900 |
| | Silicon Bronze Alloy | 2200 |
| | PMG No. 1 Metal | 1900 |
| | PMG No. 4 Metal | 1900 |
| | Hyhete Metal | 2250 |
| | Aluminum Silicon Bronze | 2000 |
| | Aluminum Iron Bronze 1. | 2000 |
| | Aluminum Iron Bronze 2. | 2050 |
| | 75000 Manganese Bronze | 1750 |
| | 100000 Manganese Bronze | 1800 |
| | Iron-Manganese Bronze | 1950 |
| | Phosphor Bronze (no added | |
| | phosphorus) | 1900 |
| | 2-B-8 Metal (no added phospho | orus) 1900 |

Preparation of the Specifications

There are a number of factors that must be taken into consideration before a specification, covering the chemical composition and the mechanical properties requirements for any single alloy, can be drawn up. Of primary importance in this connection are the following:—

- The necessity of conforming to the specification of a customer.
- The ability of the ingot manufacturer to produce ingot metal to any proposed specification with reasonable economy.
- A review of accumulated analytical data in order to obtain a picture of the limits of impurities to be expected for any given alloy as supplied by different ingot manufacturers.

 A review of the available data on ingot specifications as published by various standards bodies and by ingot manufacturers.

It is not always an easy matter for the foundry to draw up an ingot metal specification so that it will be possible, with one specification, to fulfill the requirements of each of many customers' specifications for the alloy involved. The difficulty experienced in any instance will, of course, be proportional to the rigidity and severity of the individual customer's specification. Insofar as the mechanical properties requirements are involved it is unusual for much difficulty to be encountered here, since the individual specifications usually show a marked similarity in this respect. Where chemical compositions are involved, however, a different situation is frequently found.

Three types of specification may exist in this respect where any group of customers is concerned. The first of these, that where only an optimum composition is given and where no statement is made as to the impurities or their maximum limits, is of common occurrence and presents no difficulty whatever. The second type includes those specifications where reasonable limits are given for the principal components and where the impurities are lumped together and a maximum limit for the total impurities is given. This type of specification usually presents but little difficulty to the foundry. The third type of specification is that where the ranges of percentages for the principal components are closely limited and where each of the allowable impurities is assigned a closely restricted maximum value. This latter type of specification seldom has its origin at the desk of a metallurgist and it can be fitted only with difficulty, if at all, into the foundry metal specifications system. All too often no sound basis can be uncovered for the rigidity and severity of the specification.

Whenever the writer had occasion to draw up an ingot specification that was required to fulfill the three specification types outlined in the foregoing he endeavored, first of all, where the third type was concerned, to obtain all of the concessions possible permitting a relaxation of the specification. Where these concessions were granted the work of specification writing proceeded without difficulty. Where no reasonable concessions could be obtained after pursuing a line of persuasion based on metallurgical commonsense, they could quite frequently be obtained after the customer had been advised that he would have to absorb the extra cost involved in supplying him with an alloy of high purity demanded by his specification.

Once the different specifications for any one alloy had been reduced, as far as possible, to a reasonable basis the next step was to draw up a tentative specification for ingot metal to cover the alloy in question. At this stage of the preparation of a specification the ranges of percentages for the principal components can usually be fixed in what amounts to their ultimate form, but the limits for the allowable impurities can not be rigidly fixed and a figure should be set representing only a maximum value for the total impurities.

Quite frequently, even with a customer's specification that could be considered a comparatively easy one to fill, a single factor would tend to set it outside, at least partially, of the limits of our tentative specification. A case in point might be given here. The ingot specification, drawn up for our use, to cover the familiar 85-5-5-5 alloy is almost identical with that used generally in Canada. Table 2 outlines the specification in question. The ranges of percentages given for the principal components, and the limits assigned to the allowable impurities, are such as to satisfy nearly every customer obtaining from us castings in this alloy.

TABLE 2-"B" METAL

| | Permissible Range, % | Desired Composition % |
|---------------------------|----------------------|-----------------------------|
| Tin | 4.5-5.5 | 5.0 Min |
| Lead | 4.5-5.5 | 5.0 Max |
| Zinc | 4.5-5.5 | 5.5 |
| Copper | Remainder | Remainder |
| Iron | 0.25 Max | |
| Aluminum | 0.005 Max | |
| Silicon | 0.005 Max | |
| Antimony | 0.25 Max | |
| Phosphorus | 0.05 Max | |
| Nickel | 0.50 Max | |
| Sulphur | 0.08 Max | |
| Other Impurities | 0.10 Max | |
| Scribe Yield Point | 14,000 psi Min | |
| Ultimate Tensile Strengtl | h 30,000 psi Min | |
| Elongation in 2 in. | 20.0 Min | |

In addition to this the specification fits adequately into those listed for this alloy by reputable standards bodies such as the American Society for Testing Materials and the Society of Automotive Engineers. A few of our customers, however, preferred to use a specification for the 85-5-5-5 alloy which called for a minimum of 5.0 per cent tin and a maximum of 5.0 per cent lead. We could not see where these factors were of sufficient importance to warrant any wholesale revision of our tentative specification, particularly since this would undoubtedly increase the cost of manufacturing the alloy.

We did, however, modify our specification so as to include desired composition figures of 5.0 per cent minimum tin and 5.0 per cent maximum lead. No attempt was made to have the ingot manufacturer adhere to these desired composition figures but we did find, from experience, that a fair percentage of the ingot heat shipments had tin and lead figures in good agreement with the desired figures. These particular heats were then separated in the foundry and were subsequently used in the production of castings to the higher tin specifications. The writer would mention, in passing, that the necessity of separating ingot heat shipments in this way amply justifies the adopted system of ingot metal identification.

Even where the mechanical properties of certain alloys were concerned it was frequently necessary to make special provisions in the specification, in order to satisfy the customer's demands. For example, in many instances where aluminum-iron-bronze castings were supplied to customers, it was found essential to heat treat these castings to obtain the mechanical properties demanded by the customer's specification. In view of the sensitivity of these alloys to lead, where the properties obtainable by heat treatment are concerned, the specification drawn up to cover this type of ingot metal was very definite as to the allowable maximum for the lead content. Table 3 indicates the complete specification for one of these aluminum-iron-bronze alloys.

TABLE 3-ALUMINUM-IRON-BRONZE 1.

| Pe | ermissible Range, % | Desired Composition | |
|---------------------------|---------------------|------------------------|--|
| Aluminum | 9.5 -10.0 | 10.0 | |
| Iron | 0.75-1.0 | 1.0 | |
| Copper | Remainder | Remainder | |
| Tin | 0.10 Max | | |
| Lead* | 0.05 Max | | |
| Zinc | 0.10 Max | | |
| Silicon | 0.10 Max | | |
| Phosphorus | 0.002 Max | | |
| Antimony | 0.01 Max | | |
| Nickel | 0.25 Max | | |
| Manganese | 0.25 Max | | |
| Sulphur | 0.05 Max | | |
| Test bar as-cast and cool | ed in sand:- | | |
| Scribe Yield Point | 30,000 psi Min | | |
| Ultimate Tensile Strength | 50,000 psi Min | | |
| Elongation in 2 in. | 5.0 Min | | |

* This material does not respond favorably to heat treatment when lead is present in amounts exceeding 0.10%. In view of this no shipment of this type of ingot shall be at all acceptable when the lead content exceeds 0.05%.

The necessity of excluding from the specifications factors that would tend to annul the ability of the ingot manufacturer to supply a given alloy at a reasonable cost was prominent in our minds at all times. It was recognized that there was a limit to the purity that an ingot manufacturer could provide, together with reasonable economy, for any given alloy, this limit being dictated by the nature of the materials used by him in the compounding of the alloy. Any demand for a greater purity than these materials permitted could be met only by using higher purity materials, or by making virgin metal additions, with a corresponding increase in the cost of manufacture and supply.

In order therefore to uncover what could be considered as reasonable component ranges and reasonable impurities maximums we submitted to the ingot manufacturers tentative specifications for the alloys required. They were then asked to comment on them from the point of view of their ability to conform with reasonable economy. In many cases, where the demands of the customers' specifications could still be met, the component ranges and the impurities limits were altered to agree with the comments and criticisms received.

Table 4 gives some idea of what was done in this direction. The high-lead bronze specifications were arranged so that, with increasing lead content, the permissible maximum for the antimony impurity also increased. This permitted the ingot manufacturer to use materials in producing ingot to these specifications that allowed a reasonable economy.

A second example in this connection can be given, and this is in connection with the specifications drawn

TABLE 4

| | "B" | 80-10-10 | 15% Lead | 22% Lead |
|---------------------|-----------|-------------|-------------|-------------|
| | Metal, % | Metal, % | Metal, % | Metal, % |
| Tin | 4.5 - 5.5 | 10.0 - 11.0 | 7.0 - 10.0 | 4.5 - 6.5 |
| Lead | 4.5 - 5.5 | 9.5 - 10.5 | 12.0 - 15.0 | 18.0 - 22.0 |
| Zinc | 4.5 - 5.5 | 0.50 Max | 1.0 - 2.5 | 3.0 - 4.0 |
| Copper | Remainder | Remainder | Remainder | Remainder |
| Iron | 0.25 Max | 0.20 Max | 0.25 Max | 0.25 Max |
| Aluminum | 0.005 Max | 0.005 Max | 0.005 Max | 0.005 Max |
| Silicon | 0.005 Max | 0.005 Max | 0.005 Max | 0.005 Max |
| Antimony | 0.25 Max | 0.30 Max | 0.35 Max | 0.50 Max |
| Phosphorus | 0.05 Max | 0.05 Max | 0.05 Max | 0.05 Max |
| Nickel | 0.50 Max | 0.50 Max | 0.50 Max | 0.50 Max |
| Sulphur | 0.08 Max | 0.08 Max | 0.08 Max | 0.08 Max |
| Other Impurities | 0.10 Max | 0.10 Max | 0.10 Max | 0.10 Max |

up to cover the phosphor bronze alloys. In order to avoid any difficulty in meeting the phosphorus ranges specified ingot metals of this type are ordered with a maximum phosphorus content of 0.03 per cent. The required quantities of phosphor copper are then added by the foundry when melting this ingot for castings production.

The advice of the ingot manufacturers on the subject of their ability to provide ingot metal, for any given alloy, conforming to specified component ranges and impurities limits, was always considered in conjunction with analytical data of our own. These data covered the accumulated analytical results representing our tests on ingot metals supplied to us over a period of many years.

The usual procedure here was to select a specific alloy, and to obtain from the data covering this alloy average figures for each impurity and component. These average figures, particularly those covering the impurities, were then compared with the maximum figures and the ranges suggested by the ingot manufacturers.

In the majority of cases the two sets of figures agreed closely, and this provided an additional reason for altering, where required, the figures for the specification in question. In a few instances, however, discrepancies were noted between the two sets of figures, and in these cases the specification was drawn up for the alloy involved under conditions which favored the average figure gleaned from the analytical data.

It was found, for example, that the ingot manufacturers were anxious to have the nickel impurity for the 85-5-5-5 alloy specification set at a maximum of 1.0 per cent, or at least at 0.75 per cent. A review of our analytical data indicated here that only about one heat in forty of this alloy could be expected to have a nickel content significantly in excess of 0.50 per cent. On account of these findings the specification was drawn up with a maximum limit of 0.50 per cent for the nickel content.

As a final means of judging the fairness and the universality of the reconstructed metal specifications, they were compared with similar specifications, wherever this was possible, as issued by the American Society for Testing Materials, the Society of Automotive Engineers, the Non-Ferrous Ingot Metal Institute and the Canadian Standards Association.

When it appeared that the component ranges or the impurities limits, for any alloy, were not of sufficient fairness or universality these were adjusted to conform to good practice, this providing always that the restrictions imposed by the customers' specifications permitted the adjustment. It is obvious that, on account of the individual demands of our customers, the metals specifications system now used can not be expected to agree in all respects with comparable specifications as issued by the standards bodies in question. In most cases, however, a good degree of universality and of fairness was noted.

The writer would like to stress but one more point. The final products of the reconstruction of the specifications system were eventually assembled and issued to the ingot manufacturers. The specifications involved are not looked upon as being permanently fixed and year by year the work of reconstruction goes on, revisions of the system being issued whenever necessary.

In the meantime the work of assembling a complete system of specifications covering binary alloys is proceeding apace and it is expected that this will be issued sometime in 1950. The value to the foundry of a proper metals specification system can hardly be over-emphasized. In this connection the writer would like to point out that, since the time of issue of the reconstructed metal specifications system, no heat of ingot finally accepted by the writer's organization has ever been found to give trouble in actual castings production. It is expected that the specifications covering binary alloys will produce similar results.

DISCUSSION

Chairman: W. ROMANOFF, H. Kramer & Co., Chicago. Co-Chairman: G. K. Eggleston, Barnes Mfg, Co., Mansfield, Ohio.

G. P. HALLIWELL (Written Discussion): ¹ In considering this paper, I think that it should be kept in mind that some of the recommendations contained in it are included to meet conditions which exist in Canada and not in U.S.A.

In the first place, a comparison of the composition of some of the alloys given in Tables 2, 3 and 4 of the paper will show that their compositions differ in many cases from similar ones in this country, not only in the range of the major elements but also in the minor elements.

Secondly, Canadian ingot manufacturers do not have the furnace capacity of the larger smelters in this country and modifications are made to conform with this capacity.

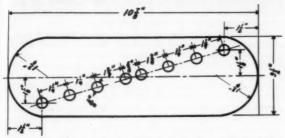
Thirdly, the fact that the Canadian Bronze has found it necessary to spend a considerable amount of time to set up a rigid set of specifications, covering the purchase of their own material, they are for the most part within the framework of the specifications written by the above-mentioned organizations. It is rather difficult to see how smaller foundries in Canada could follow out all the recommendations in this paper.

In Clause 4 of the paper, it is stated that the fractures of the ingot should have no indications of any porosity, holes or discolorations. While we recognize the value of information gained from fracture tests in general, the interpretations of such may be misleading. Ingots, unlike castings, are not fed from a riser, and may develop shrink and porous centers. Sometimes ingots are poured rapidly at a higher temperature than normal into ingot molds which have not been cooled sufficiently. The rapid burning of charcoal entrapped between the ingot mold and partially solidified ingot, generates a gas, probably carbon monoxide, which becomes entrapped there as bubbles and leave their imprint on the sides and bottom of the ingot. Occasionally, these extend 1/4 in. or more into the ingot, but should not be taken as an indication of gassy metal and thereby subject to rejection.

¹ Director of Research, H. Kramer & Co., Chicago.

In Clauses 5 and 6, it is stated that the ingot must have a uniform composition. This statement disregards the metallurgical phenomenon called inverse segregation, which is prone to occur in Copper-Tin, Copper-Tin-Lead and Copper-Tin-Lead-Zinc alloys. The method used by the author for sampling an ingot for chemical analysis places undue weight on chips taken from those portions of the ingot where inverse segregation is most likely to occur.

As a result of a considerable amount of work on sampling ingots for chemical analysis, we drill four holes completely through the ingot along one half the diagonal of the ingot. This is seen in the accompanying figure. Since the first hole represents only



about one half the thickness of the ingot, a proportionate amount of these chips is taken and mixed with the proper portion from the remaining holes. Since one half the ingot may be assumed to freeze the same as the other, four sets of samples may be obtained on an ingot, if necessary, in case of dispute, by drilling along the respective diagonals. We have found that this method more nearly represents the composition of the entire

ingot than any other method of sampling.

The requirements suggested in Clause 8 seem to put an unwarranted burden on the ingot maker. Emphasis is placed on the necessity for complete testing by the foundry, of representative ingots for both chemical and mechanical properties before shipment is made. This would work a definite hardship both to ourselves and our customers. Many times orders are obtained with a request for immediate shipment. This is often done within 24 hr after the metal has been made and our own tests completed. It is not practical to ship a few ingots a few hundred to several thousand miles for tests, while holding the remainder in storage and awaiting the results of further tests. A certified analysis by the supplier as to composition and properties of the metal should be sufficient.

In the procedure outlined for melting and pouring of test bars, no mention is made of a deoxidizer. It is general practice in this country to use phosphor copper to deoxidize and increase the

fluidity of the molten metal.

The type of test bar is another point that differs between the two countries. I do not wish to start a prolonged discussion at this time on this subject. I prefer simply to call attention to the prime purpose of a test bar, namely, to determine the quality of the metal when it was molten and its ability to develop good castings as measured by the mechanical properties obtained. Extensive tests made by Battelle Memorial Institute on research for the Brass and Bronze Institute and in our own laboratory, have shown that the keel block does not, in our estimation, test the quality of the heat from the standpoint of its gas content. Other investigators have shown that the mass effect, which is so marked in the keel block, greatly affects the mechanical properties. Consequently, most specification bodies in this country require a test bar casting of a smaller mass of metal, well fed from the point of view of directional solidification and generally cast to size or approximate shape, and then machined to 0.505-in. diam.

In addition, the keel block test bar is not suitable for heattreatable aluminum bronzes, nickel bronzes or any alloy sensitive to heat-treatment. The long period of cooling serves as an aging

reatment.

In describing the three types of specifications that have been encountered by the author, he definitely does not appear to be in favor of the third type. It may be that I have misread his paper, but it seems to me that this is the very method followed in this country by all our standard and reputable specification making bodies. Certainly, it is not the first or second type. The lumping of impurities together at a maximum amount is wrong, because any single impurity can exceed an amount that is generally considered undesirable. It is far better to set a maximum

amount of total impurities and also a maximum for the individual impurities. This is the type of specification written by ASTM, SAE and the various branches of the government.

In this country, it is generally accepted that zinc is taken as the balance of the analysis, rather than copper. The chief reasons for this are:

1. Zinc is lost in volatilization during melting and is far more

difficult to hold within narrow limits than is copper.

2. Copper can be quickly and accurately determined in the chemical laboratory, whereas the determination of zinc will take

two or three times as long at best.

We are also interested in the specification for aluminum at 0.005 per cent maximum, from the standpoint of analysis. The British have deleted it from their specifications at that figure, because they have no method that will analyze correctly for it at that concentration. ASTM set a minimum of 0.005 per cent determined on a 10-gram sample, but have no recommended standard analysis for any concentration below 0.10 per cent. Several smelters in this country are using the spectrograph or spectrophotometer based on synthetic samples in an aqueous solution made from pure aluminum or pure aluminum salts. I wonder how the author controls this problem.

I would like to suggest that more specific information be given on the composition of the alloys in Table 1 which are not included in Tables 2 to 4. I think that it would be extremely beneficial to the non-Canadian reader who would then be able to

classify the alloy.

AUTHOR'S REPLY TO MR. HALLIWELL

MR. DICK (Written Reply to Mr. Halliwell): I shall endeavor to comment briefly on the points brought forward by Mr. Halliwell.

The statement made to the effect that the compositions shown in Tables 2, 3 and 4 of the paper differ in many cases from similar ones used in the United States is certainly true but in a limited sense only. Let me cite two examples in explanation. Shown below are the 85-5-5-5 and the 78-7-15 compositions as set forth in our specifications and as outlined in the July 1st, 1947 revisions of the NFIMI.

| | 85-5-5-5 | | 78-7-15 | |
|------------|-----------|-------------|-----------|-----------|
| | Our Spec. | NFIMI | Our Spec. | NFIMI |
| Tin | 4.5-5.5 | 4.25-5.25 | 7.0-10.0 | 6.5-7.5 |
| Lead | 4.5-5.5 | 4.50 - 5.50 | 12.0-15.0 | 14.0-16.0 |
| Zinc | 4.5-5.5 | 4.50 - 5.75 | 1.0-2.5 | 0.75 Max |
| Copper | Remainder | 84.00-85.00 | Remainder | 76.0-79.0 |
| Antimony | 0.25 Max | 0.25 Max | 0.35 Max | 0.75 Max |
| Nickel | 0.50 Max | 0.75 Max | 0.50 Max | 0.75 Max |
| Iron | 0.25 Max | 0.25 Max | 0.25 Max | 0.10 Max |
| Silicon | 0.005 Max | 0.003 Max | 0.005 Max | 0.003 Max |
| Aluminum | 0.005 Max | 0.005 Max | 0.005 Max | 0.005 Max |
| Sulphur | 0.08 Max | 0.08 Max | 0.08 Max | 0.08 Max |
| Phosphorus | 0.05 Max | 0.03 Max | 0.05 Max | 0.03 Max |

Yield Point, psi 14,000 Min 17,000 Min 14,000 Min 14,000 Min Tensile Strength,

psi 30,000 Min 33,000 Min 24,000 Min 25,000 Min Elongation in 2 in. 20.0 Min 20.0 Min 15.0 Min 10.0 Min

Where the respective compositions for 85-5-5-5 are concerned it will be seen that, for the major elements, the main deviation lies in the range for tin. Even here, however, the difference is of a very minor nature. Where the minor elements are involved the only important difference is in the allowable nickel content, our specification permitting slightly less nickel than that of the NFIMI. I have already stressed this point in my paper and made clear therein the reason why the lower nickel maximum was chosen.

A comparison of the two compositions for the 78-7-15 alloy indicates several differences where the major elements are concerned. Our tin range allows a greater latitude but demands a higher minimum figure. Our zinc is given as a range and not as a maximum. Both of these factors are set by our customer's specifications and not by our own choice. Our lead range, on the other hand, permits a greater latitude, and this is definitely an advantage. Where the minor elements are concerned the main points at variance are those of the nickel and antimony maxima, our allowable percentages for these elements being lower than those of the NFIMI. Once again I might mention that customer's specifications dictate that these maxima be fairly closely confined.

The second point mentioned by Mr. Halliwell is that difference in furnace capacity, between the American and Canadian ingot manufacturers, has necessitated modifications in our specifications in order that the Canadian manufacturers may conform within their more limited furnace capacities. This is certainly true, particularly insofar as Clause 1 of our specifications is concerned.

Mr. Halliwell's third point introduces a rather debatable point inasmuch as it seems to imply that the main reason behind the setting up of our specification system was the lack of central specification bodies. That this undoubtedly existed as a contributing reason is true. The main reason, however, was to ensure that any ingot metal supplied to us would be capable of meeting the specifications of our customers and, in addition, be of a nature such as to provide good castings.

INTERPRETING FRACTURES

The fourth point brought forth in the discussion is that of the technique of interpreting the fractures obtained as a result of the ingot fracture test. Undoubtedly a misunderstanding exists here, and this may easily be due to the fact that our Clause 4 does not completely describe the means used to interpret ingot fractures. In order to clarify this point I would mention that discolorations and minor porosity in the neighborhood of the top of the ingot, due to shrinkage and a lack of feeding, are not considered factors to cause ingot rejection. Similarly gas or air entrapped at the sides or bottoms of ingots does not constitute a reason for rejection. This type of porosity is, as mentioned by Mr. Halliwell,

certainly not indicative of gassed metal.

Mr. Halliwell's statement to the effect that our Clause 5, which demands uniformity of composition in ingot metal, ignores the phenomenon of inverse segregation is definitely the result of a misunderstanding on his part. The reference to the need for the uniform composition in our ingot may be explained as follows. We procure, let us say, five ingots from the beginning of a furnace pour at the ingot manufacturer's plant, five ingots from the middle pour and five ingots from the final pour. We then drill each of the five ingots from the initial pour, mixing these drillings to obtain one composite sample. This is repeated for the ingots from the middle and final pours. We thus obtain three representative samples and these, upon chemical analysis must show results which lie within the respective ranges permitted by the specifications. This is what we mean by uniformity of composition. We do not expect that each individual ingot will show a uniform composition over all the points of that ingot.

METHOD OF SAMPLING

Where the method of sampling is concerned I can only state that we have repeatedly, in the past, compared our method of sampling with that outlined by Mr. Halliwell and have yet to obtain, as a result of careful chemical analysis, results showing

any appreciable or significant variation.

The remarks made concerning the burden placed on the ingot maker by the details of our Clause 8 would appear to be somewhat misdirected. If the customer is willing to set up this system and to assume the burden of the extra work entailed thereby, always with the option of waiving the procedure in times of emergency when ingot is urgently required, then no hardships should exist for the ingot maker. Undoubtedly, as mentioned in the paper, an additional degree of protection is afforded both maker and user by the use of this system.

Mr. Halliwell has mentioned the omission of a deoxidizer in our procedure for melting and pouring the test bar. Since only 100 to 125 lb of ingot is usually melted for the test bar, and since the rate of melting is very rapid and takes place in a covered crucible, we have usually counted on there being sufficient residual phosphorus in the ingot to take care of any deoxidizing process. In addition to this we prefer to refrain from adding any materials to ingot metal while melting and pouring for test bars connected with the purchase and acceptance of ingot metal. This applies, for example, in the case of phosphor bronze alloys, where the ingot is ordered without its phosphorus content and tested without any addition of phosphor copper, and also in the case of manganese bronze alloys, where the test bars are cast without any prior addition of zinc to take up melting losses. We have never yet experienced any difficulty by the omission of these additions.

I would like it understood, however, that under regular foundry conditions all of our castings and test bars are derived from properly deoxidized heats and from heats to which the necessary additions have been made. These additions are omitted only when we prepare test bars for testing in connection with the purchase and acceptance of ingot metal.

The matter of the size and shape of our test bar keel block, in comparison to those of a test bar casting representative of the ideas of some of the specification bodies in the United States, is one liable, as Mr. Halliwell has remarked, to lead to prolonged and unnecessary discussion. The only point I will stress, in answer to Mr. Halliwell's objections to the keel block, is that we have found it to yield satisfactory results with ordinary bronzes, phosphor bronzes and high shrinkage alloys, including heat treatment aluminum bronzes and nickel bronzes. The remarks made concerning the tendency for the mass effect of the keel block to

cause some aging are, of course, quite true.

In describing the paper as being opposed to the third type of specification, that embodying closely confined ranges for the principal elements and closely restricted maxima for the impurities. Mr. Halliwell has indeed misinterpreted the point at issue, although this may well have been due to a lack of expansion by the writer on this subject. The type of specification to which I had reference was that were the ranges for the principal elements, and the maxima for the impurities, were of a nature so rigorous as to render it difficult to meet the specification economically. We have had, for example, cases where we have been asked to supply a high lead alloy with a range of only plus or minus 0.5 per cent on the lead content and a maximum of 0.1 per cent for the antimony. This is the type of specification to which I had reference and I am sure Mr. Halliwell will agree with me when I say that this would be a specification difficult to fulfill with any economy. I feel that this question is one that could have been avoided by a more careful examination of our own specifications, examples of which were given in the paper. Those examples, I think, show adequately that we agree with Mr. Halliwell and with ASTM and SAE as to the form of specification to which we subscribe.

Insofar as the zinc content is concerned this is determined by us by difference during a chemical analysis. It still must, of course, conform to a specified range. Although our specifications show copper as a remainder in most cases, the copper determination is actually made and the zinc figure obtained finally by

difference.

Where the determination of aluminum is concerned, as this applies in the case of maxima of 0.005 per cent, I might point out that, in agreement with one of the suggestions made by Mr Halliwell, this is done spectrophotometrically, and graphs are used, these having been prepared by the use of readings obtained on synthetic aqueous-base samples containing variable quantities of pure aluminum salts.

Finally in answer to Mr. Halliwell's request for further information in connection with the compositions of the alloys shown in Table 1 of the paper, I have shown below the alloys in question in opposition to, wherever possible, similar alloys

from the NFIMI listing or the ASTM listing.

| Our Alloy | ASTM or NFIMI Listing |
|----------------------------|--------------------------------|
| "A" Metal | NFIMI Desig. 2A 88-6-11/2-41/2 |
| "B" Metal | NFIMI Desig. 4A 85-5-5-5 |
| 15% Lead Metal | NFIMI Desig. 3D 78-7-15 |
| 22% Lead Metal | NFIMI Desig. 3E 70-5-25 |
| 80-10-10 Metal | NFIMI Desig. 3A 80-10-10 |
| 8-15 Metal | Approx. same as 15% Lead Meta |
| Gun Metal | ASTM B143-49 Class 1A |
| 90-10 Metal | 10 Tin 90 Copper |
| Silicon Bronze Alloy | No comparable alloy |
| P.M.G. No. 1 Metal | As in United States |
| P.M.G. No. 4 Metal | As in United States |
| Hyhete Metal | No comparable alloy |
| Aluminum Silicon Bronze | No comparable alloy |
| Aluminum Iron Bronze No. 1 | NFIMI Desig. 9B 89-1-10 |
| Aluminum Iron Bronze No. 2 | NFIMI Desig. 9A 871/2-31/2-9 |
| 75,000 Manganese Bronze | NFIMI Desig. 8A |
| 100,000 Manganese Bronze | NFIMI Desig. 8C |
| Iron Manganese Bronze | No comparable alloy |
| Phosphor Bronze | 6 Sn, 93.5 Cu, 0.5 P |
| 2-B-8 Metal | 11 Sn, 88.25 Cu, 0.75 P |

I would like, in closing, to thank Mr. Halliwell for his lively interest in the paper and to express the hope that the foregoing has answered satisfactorily the questions put in his discussion.

A STUDY OF FACTORS AFFECTING MOLDING SAND DENSITY, SHRINKAGE, EXPANSION AND WORKABILITY

By

R. P. Schauss, R. F. Baley* and E. E. Woodliff**

ABSTRACT

In order to gain a better understanding of the mechanics of foundry sands, their effects upon moldability and casting quality, the authors undertook a study of sands bonded with five common clay type binders.

The results of the investigations show rammed density of molding sand to be affected by the type of clay bond and the amount of clay substance.

Included in the investigation are results of testing sands at constant density ramming. This not only suggests a new method of testing molding sands, but enables one to measure the relative energy of ramming for different types of sands and binders.

The study includes results of tests for free and confined expansion. The results of using machined carbon specimen tubes in which the specimen is rammed for confined expansion are explained. A relationship was established between these high temperature properties and clay contents.

THE PURPOSE OF THIS STUDY is founded upon the authors' work which has indicated the advantages of carrying a substantial quantity of clay substance in molding sand. This is present in amounts over the minimum required to maintain working strength. It is felt that clay in a molding sand should act both as a binder and as a filler. The amount of clay present should be sufficient to absorb much of the expansion of the silica, thereby stabilizing it sufficiently to reduce the need for combustible and organic fillers. Thus, a molding sand bonded with a medium strength clay would carry greater clay substance, be more thermally stable and be easier to maintain at workable moisture contents.

It was first necessary to select a representative number of clays which would give the study a range of molding sands with varying clay contents. It was established that all synthetic molding sand mixtures would be bonded to give 10 psi green compressive strength. They would be a mixture of Ottawa washed and dried sand of approximately 75 grain fineness (Table 1), Clay bond, 2 per cent "E" Grade seacoal, and be tempered close to normal working moistures. All the molding sand mixtures were mixed in a muller type sand mill and riddled through a No. 6 riddle before testing.

The report has been separated into the following parts:

Part 1-Rammed densities of 10 psi bonded molding sands when different clays are used.

Part 2-Development of tests by constant density ramming.

Part 3—Establishment of the relative workability of molding sands bonded with different types of clays.

Part 4—The measurement of free expansion, contraction and shrinkage of the sands as they are affected by clay substance.

Part 5—Confined expansion testing of molding sands and the relationship of this property to clay substance.

Testing Methods and Equipment—It may be assumed throughout the report that the testing methods and the equipment used are all standard unless otherwise explained in connection with the reported tests.

PART 1

Rammed Densities of 10 psi Bonded Molding Sands When Different Clays Are Used

The term, "rammed density," applied to molding sand is the weight of sand per cubic foot as determined by the weight of the A.F.S. test specimen. Since this

TABLE 1-MATERIALS USED FOR THE TESTS

| Types of Clays and districts from 1. Pulverized Fire Clay | which they are mined. |
|--|-----------------------|
| 2. Pulverized Illite Clay | Illinois |
| | 0.717770 |
| 3. Pulverized Fire Clay | Illinois |
| 4. Western Bentonite | Wyoming |
| 5. Southern Bentonite | Mississippi |
| Sieve Analysis of the washed a | nd dried sand used. |
| Sieve No. | Per Cent Retained |
| 6 | 0.0 |
| 12 | 0.0 |
| 20 | 0.0 |
| 30 | 0.0 |
| 10 | 0.0 |
| 50 | 1.0 |
| 70 | 25.4 |
| 100 | 51.1 |
| 140 | 18.3 |
| 200 | 3.2 |
| 270 | 0.6 |
| | 0.4 |
| pan | 0.7 |

[•] Foundry Engineers, Illinois Clay Products Co., Joliet, Ill.

^{**} Owner-Consultant, Foundry Sand Service Engineering Co.,

is a function of ramming, it may be determined by a density indicator attachment to the standard rammer or may be calculated from the weight of the 2-in. long standard sand test coupon. To calculate the rammed density use the weight of sand in grams required to make the 2-in. specimen, ramming this sand to within plus or minus 0.005 in. of the full 2-in. length. This weight of sand is multiplied by the factor 0.6068, thus;

168 grams (specimen weight) \times 0.6068 = 101.9 lb/cu ft (density). Tests of five clays when used to produce molding sands of equal grain strength of 10 psi green compression are as follows:

Discussion of Results

These data substantiate results normally found with the use of these clays in production. The relationship between rammed density and clay content follows closely the conclusions of Dietert, Fairfield, and Hasty¹ and by Briggs and Morey.² In Table 2 the correlation between rammed density and clay content is more closely related to the determined A.F.S. clay than it is to the amounts of clay added to the molding sand.

TABLE 2—PHYSICAL PROPERTIES OF MOLDING SANDS, BONDED WITH THE CLAYS BEING STUDIED TO 9 TO 10 PSI

| Type of clay used | Pulv. Fire Clay Ohio | Pulv. Illite Clay Ill. | Pulv. Fire Clay Ill. | West. Ben- tonite | South. Ben- tonite |
|---------------------------|-------------------------------|---------------------------------|-------------------------------|-------------------------|--------------------------|
| Amount of clay used, % | 15.6 | 10.9 | 19.0 | 6.0 | 6.6 |
| Amount of Seacoal, % | 2.0 | 2.0 | 2.0 | 2.0 | 2.0 |
| Moisture (test), % | 4.40 | 4.15 | 5.20 | 3.20 | 3.90 |
| Permeability | 32.5 | 63.7 | 32.9 | 81.0 | 87.0 |
| Green Strength, | | | | | |
| Compression, psi | 9.7 | 9.6 | 9.4 | 9.3 | 9.6 |
| Rammed Density, | | | | | |
| lb/cu ft | 108.0 | 100.5 | 108.5 | 96.0 | 95.2 |
| Flowability, % | 78.2 | 75.2 | 74.0 | 76.0 | 73.5 |
| Dry Strength, comp., psi | 75.4 | 68.0 | 87.2 | 93+ | 73.0 |
| Dry Strength, shear, psi. | 17.7 | 19.2 | 22.0 | 37.1 | 19.3 |
| Mold Hardness | 85 | 85 | 85 | 85 | 85 |
| A.F.S. Clay, % | 15.2 | 10.7 | 15.60 | 6.7 | 6.8 |
| No. Rammer Blows | 3 | 3 | 3 | 3 | 3 |

TABLE 3-MOLDING SAND DENSITY EFFECTED BY SOME COMMON SAND ADDITIVES (Rammed density, lb/cu ft)

| | Base Mixture | 7% Seacoal | 0.5% Cereal | 1% Cereal | 1% Woodflour |
|----------------|-----------------|---------------|----------------|--------------|-----------------|
| Pulverized | 108.0 | 105.0 | 106.3 | 104.2 | 108.0 |
| Fire Clay-Ohio | | | | | |
| Pulverized | 100.5 | 99.5 | 100.7 | 99.0 | 101.2 |
| Illite Clay | | | | | |
| Pulverized | 108.5 | 105.0 | 106.8 | 104.2 | 107.2 |
| Fire Clay-II. | | | | | |
| Western | 96.0 | 96.5 | 95.3 | 95.3 | 96.2 |
| Bentonite | | | | | |
| Southern | 95.2 | 95.8 | 95.3 | 95.0 | 95.2 |
| Bentonite | | | | | |

From the data it may be seen that rammed density is directly opposite from permeability. This is to be expected when one considers that a sand of high density is more closely rammed together, other factors, such as grain size and strength being equal. Mold hardness does not vary with a change in rammed den-

sity. In Part 2 this is brought out more clearly when density is made to remain constant. Flowability, as measured, does not parallel the rammed density of the sands. In view of the fact that rammed density is the weight of the sand which is rammed into a given volume, a change in measured density would then be a sensitive measurement of flowability.

How Some Other Materials Affect Sand Density—In an attempt to study the effects of some other sand additives on rammed density additions were made to the above base mixture in Table 2. This series of tests was made and a summary of the results is given in Table 3.

Conclusions

1. In these tests where the moisture is held at practical molding range and grain distribution, green strength and ramming energy were all held equal, the rammed density varied with the type and amount of clay substance as determined by the A.F.S. clay test.

Mold hardness and flowability, which are practical tests, were found not to be related to rammed densities of sands studied.

3. Most additives studied, i.e., seacoal, cereal flour and wood flour, showed a tendency to reduce the rammed density of all types of clay bonded sands. Although it is to be noted that each type of clay held its respective position with each material. Wood flour was found to be the least effective in bringing about a change in density.

PART 2

Development of Tests by Constant Density Ramming

The term "constant density ramming," is perhaps new, but simply implies that in ramming the test specimen for molding sand tests some predetermined weight of sand be taken as the constant weight of the specimen. It is made by deviating from the standard three rammed blows and noting the number of rammer blows and/or fractions thereof needed to ram the specimen to its full 2-in. length. The number of rammer blows together with the specimen weight, or calculated density in terms of pounds per cubic foot, gives the two additional properties obtained in this method of test-

TABLE 4—PHYSICAL PROPERTIES OF MOLDING SANDS BONDED TO 9 TO 10 PSI UNDER STANDARD RAMMING, BUT TESTED UNDER CONSTANT DENSITY RAMMING

| | Pulv. Fire Clay Ohio | Pulv. Illite Clay Ill. | Pulv. Fire Clay Ill. | West. Ben- tonite | South. Ben- tonite |
|---------------------------|-------------------------------|---------------------------------|-------------------------------|-------------------------|--------------------------|
| Amount clay used % | 15.6 | 10.9 | 19.0 | 6.0 | 6.6 |
| Amount of seacoal, % | 2.0 | 2.0 | 2.0 | 2.0 | 2.0 |
| Moisture (Test), % | 4.5 | 4.3 | 5.25 | 3.4 | 3.8 |
| Permeability | 55.7 | 59.3 | 60.8 | 50.5 | 49.4 |
| Green Strength, | | | | | |
| Compression, psi | 6.2 | 10.3 | 5.8 | 12.7 | 14.9 |
| Rammed Density | | | | | |
| lb/cu ft | 102.0 | 102.0 | 102.0 | 102.0 | 102.0 |
| Flowability, % | _ | _ | | _ | - |
| Dry Strength, Comp.,psi | 50.5 | 76.4 | 64.0 | 93+ | 93+ |
| Dry Strength, Shear, psi | 11.8 | 20.5 | 17.7 | 63.9 | 27.0 |
| Mold Hardness | 78 | 88 | 80 | 91 | 92 |
| A.F.S. Clay, % | 15.2 | 10.7 | 15.6 | 6.7 | 6.8 |
| Number of Rammer blows | 11/4 | 33/4 | 11/4 | 9 . | 10 |

ing. Other standard physical tests made on specimens rammed to constant density show some new relation-

ships between different types of clay.

In establishing the value for constant density, a figure close to the average density of the five clays was taken. Then for constant density ramming all sands were rammed to this value. It served as a common denominator for testing of the molding sands. The value used throughout these tests is 102 lb per cu ft (168 grams, specimen weight). A record of the ramming energy, i.e. number of rammer blows, is made part of the data as in Table 4.

In Table 5, data is given for two clays in sand mixtures bonded to give a green strength reading of 10 psi under the constant ramming method. Due to the change in quantities of clay additions it was found necessary to make slight adjustments in tempering water, otherwise the mixtures tested were the same as

those in Table 4.

TABLE 5-TEST RESULTS OF TWO CLAYS OF WIDELY DIFFERENT RAMMED DENSITIES, BONDED TO 10 PSI GREEN COMPRESSIVE STRENGTH UNDER CONSTANT DENSITY RAMMING

| | Pulv. Fire Clay Illinois | Western Bentonite |
|----------------------------------|--------------------------------|----------------------|
| Amount Clay Used, % | 27.0 | 3.8 |
| Amount of Seacoal, % | 2.0 | 2.0 |
| Moisture (test), % | 5.40 | 2.60 |
| Permeability | 45.6 | 49.6 |
| Green Strength, Compression, psi | 10.1 | 9.8 |
| Rammed Density, lb/cu ft | 102.0 | 102.0 |
| Flowability, % | _ | _ |
| Dry Strength, Comp.,psi | 74.5 | 93+ |
| Dry Strength, Shear, psi | 16.0 | 51.2 |
| Mold Hardness | 85 | 87.5 |
| A.F.S. Clay, % | 20.5 | 5.0 |
| Number of Rammer blows | 1% | 12 |

Discussion of Results

A comparison of the results obtained by standard ramming with those obtained by constant density ramming may be seen in Tables 2 and 4. First, it is well to point out that clays which have high normal rammed densities with standard ramming required less than three rammer blows when rammed by the constant density method. Those of normal low density required more than the three rammer blows. The test results of the clays studied show physical properties widely different from those obtained from the standard method of testing. From the physical property data obtained by constant ramming of fire clay and bentonite bonded sands it is shown that green permeability varies only slightly and that green strength, dry strength and mold hardness vary as the ramming. Under constant density ramming those clays which have higher normal densities test weaker in strength and hardness than the low density clays.

A brief study is given (Table 5) where the properties of fire clay bonded sand are compared to a western bentonite sand when both are bonded to 10 psi green compressive strength under constant density ramming. Here the green permeabilities, green strength and mold hardness values were found to be about equal.

The quantity of fire clay used was about seven times greater than western bentonite on a weight basis. At the same time the ramming energy required for forming the bentonite bonded sand specimen was about seven times greater than for the fire clay bonded sand.

Much has been written during the past few years with regard to the increase of molding sand density by adjusting grain distribution. This phase was not touched upon in this study. In Table 2 it may be noted that density for low clay content sands is 96 lb per cu ft, while the density for the higher clay content sands is 108 lb per cu ft. These tests definitely show that increased density may be had through the selection of bonding materials. The difference between high and low clay content sands is 14 per cent.

Conclusions

1. Testing of sand under conditions of constant density ramming will supply valuable information to the foundryman. It forms a common denominator for the testing of all types of foundry sand.

Number of rammer blows or ramming energy required to form a standard size test specimen can be interpreted as a measure of moldability or flowability

of molding sand.

3. Tests show that permeability is more a function of rammed density than of clay content when the base sand is held constant.

Green strength, dry strength and mold hardness values are functions of the energy used in ramming the test specimen.

PART 3

Establishment of Relative Workability of Molding Sands Bonded with Different Types of Clays

It has been pointed out in Parts 1 and 2 that different types of clays used for bonding molding sands exert a marked influence on the weight of sand which can be rammed into a given volume. Transposing this into practical terms, it is the number of jolts required to ram a flask to strike-off level with different types of sand, always ramming the flask to the same weight for each type of sand. This is basically what the authors have been doing in determining the work which has been required to ram an equal weight of the different bonded clay sands into a given volume. The results have been determined and are tabulated in Table 6.

Discussion of Results

While the information given here may seem academic in interest to some readers it is actually of a

TABLE 6—MEASURE OF RAMMING ENERGY OF MOLDING SANDS BONDED TO 10 PSI GREEN COMPRESSIVE STRENGTH, USING A.F.S. 2-IN. DIAM x 2 IN. LONG TEST COUPON

| | Standard Ramming | Density by Const. Den. Ramming Lb/Cu Ft | | Ramming Energy (work) InLb |
|--------------------------|---------------------|--|-------|-------------------------------------|
| Pulv. Fire Clay Ohio | 108.0 | 102.0 | 1.25 | 35 |
| Pulv. Illite Clay | 100.5 | 102.0 | 3.75 | 105 |
| Pulv. Fire Clay Illinois | 108.5 | 102.0 | 1.25 | 35 |
| Western Bentonite | 96.0 | 102.0 | 9.00 | 252 |
| Southern Bentonite | 95.2 | 102.0 | 10.00 | 280 |

practical nature. Dietert, Fairfield and Hasty's paper,¹ "Density of Molding Sand Part I" shows the bearing which density of rammed sand has on the solidification rates of metal. The second paragraph of the conclusions, page 179, is quoted. "The density of molding sand, whether controlled by the degree of ramming or by choice of ingredients, exerts sufficient influence on the rate of metal solidification of gray iron to make it of practical importance. As the density of sand increases, the rate of metal solidification increases. Thus the density of the sand may affect the structure of the metal and the manner in which the casting shrinks."

The authors' experience has shown that the above holds true and that increased density of molding sand materially reduces metal penetration on iron, steel and bronze castings. The problem of increasing mold density may often be solved through the addition of selected bonding materials instead of increasing it by raising mold hardness with further ramming.

An equally important influence of molding sand density is the amount of work or ramming energy required to mold the sands with different density characteristics. Applying this to production work where the time of mold ramming is of importance, we compare the number of rammer blows found for the specimens formed in the tests of Table 6. Here the ramming energy is figured in inch-pounds by using the weight of the standard rammer (14 lb) falling a distance of 2 in.; one full rammer blow then amounts to 28 in.-lb. In constant density ramming this is multiplied by the experimental results, or number of rammer blows, to give the total ramming energy to produce the test specimens of equal size and weight. In terms of production it can be seen that from seven to eight times more work is needed to mold the sand to a constant density of 102 lb per cu ft when bentonite is used as a bond than when fire clay is used as a binder.

Conclusions

1. Density of rammed sand is an influencing factor in casting production. The experimental work contained in this report coincides closely with the work of others.^{1,2}

2. This report has served to offer further proof that density is in part a function of the type of clay bond in a molding sand mixture. Since density is a property of importance, it is then our problem to show some of the related properties. One means of accomplishing this is to mold the rammed density constant and determine the factors which are changed. The most outstanding factor has been the measurement of the work required to mold sands of different density values. This might well be called a "workability factor."

PART 4

Measurement of Free Expansion, Contraction and Shrinkage of Sands As They Are Affected by Clay Substance

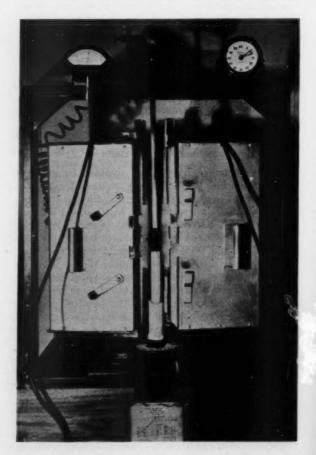
In the study of types of clays, their properties and behavior as bonds for molding sands the authors have not neglected the study of them at elevated temperatures. Instead, this phase was considered of such importance that it has been set aside from the work on physical properties to give it ample study.

The purpose of tests at elevated temperatures was to determine what relationship existed between the quantity and the types of clays and the molding sands thermal behavior. The thought has often been expressed that expansion of sand is a function of silica and that contraction or shrinkage is a property related to the clay and the fluxing action of the low melting constituents in the sand or clay, or both.

The latter condition has been held to a minimum by using a washed and dried silica sand. Seacoal was held at 2 per cent, with the only variables being the types and quantity of clay substance and tempering water. Distilled water was used in all tests.

Sand mixtures were substantially those reported ir Parts 1 and 2 of this report. In addition mixtures of molding sand were made using a range of the separate clays to give green strengths from 3 to 16 psi green compressive strengths. All these mixtures were prepared in a laboratory-size muller-type mixer and riddled before testing.

For the expansion tests standard 11/8-in. diam x 2 in. long specimens were made by three rams of the A.F.S rammer for this specimen. The dilatometer furnace used is an electrically heated furnace capable of heating to 2600 F. This furnace, Fig. 1, has a hinged back which enables it to open similar to a book. The tour



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Fig. 1-View of dilatometer furnace, booking type, partly open at completion of a free expansion test.

casters and three guide studs gives it a prescribed path to roll forward and backwards, following a slotted guide plate. The design of the furnace is to allow the specimen to be set in place for testing and the heated furnace then closed around it. Tests for expansion in this work were made at 2500 F. Readings were taken from the dial indicator through its contact with the specimen with the fused quartz rod. With the aid of the stop clock, readings were taken at exact time intervals over a test period of 12 min at heat.

Failures of Free Expansion Tests

Exhaustive tests were made in the dilatometer furnace to obtain what is termed "free expansion," "free contraction" and "shrinkage." The latter property being the difference between expansion and contraction readings. The results of the tests for free expansion seemed to show little or no correlation. After extensive testing along this line it was concluded that the free expansion reading was of little value. The reason for this contention was that practically all the specimens would crack when exposed to the oxidizing atmosphere of the 2500 F furnace. This led the investigation into confined expansion testing. This will be explained in Part 5 of this report.

The free expansion test has merit for it enables us to measure the shrinkage property of molding sand. It appears that this property is a function of the silica, clay substance and fluxes present in the molding mixture. The shrinkage values for the clay under study together with the clay substance as determined on the

TABLE 7-SHRINKAGE VALUES FOR MOLDING SAND MIX-TURES. FREE EXPANSION TESTS, 2500 F

| Pulve | rized Fire | Clay-Ohio |) | |
|------------------------|-------------|------------|------------|--------|
| Clay Additions, % | 5.6 | 10.6 | 15.6 | 20.6 |
| Shrinkage, in./in. | 0.0267 | 0.0512 | 0.0517 | 0.0471 |
| A.F.S. Clay, % | 6.3 | 10.7 | 15.2 | 19.6 |
| Density, lb/cu ft | 101.6 | 104.6 | 108.0 | 109.5 |
| Pul | verized Ill | ite Clay | | |
| Clay Additions, % | 4.9 | 7.9 | 10.9 | 13.9 |
| Shrinkage, in./in. | 0.0477 | 0.0540 | 0.0745 | 0.0730 |
| A.F.S. Clay, % | 5.6 | 8.1 | 10.7 | 14.0 |
| Density, lb/cu ft | 99.0 | 99.8 | 100.5 | 101.0 |
| Pulveri | zed Fire C | lay-Illino | is | |
| Clay Additions, % | 6.0 | 13.0 | 19.0 | 27.0 |
| Shrinkage, in./in. | 0.0320 | 0.0345 | 0.0654 | 0.0482 |
| A.F.S. Clay, % | 5.7 | 10.6 | 15.6 | 20.5 |
| Density, lb/cu-ft | 100.0 | 103.7 | 108.5 | 108.5 |
| W | estern Ber | tonite | | |
| Clay Additions, % | 3.8 | 6.0 | 8.8 | |
| Shrinkage, in./in. | 0.0124 | 0.0415 | 0.0250 | |
| .F.S. Clay, % | 5.0 | 6.7 | 8.5 | |
| Density, lb/cu ft | 96.2 | 96.0 | 95.2 | |
| So | uthern Be | ntonite | | |
| Clay Addition, % | 6.6 | | | |
| hrinkage, in./in. | 0.0220 | | | |
| I.F.S. Clay, % | 6.8 | | | |
| Density, lb/cu ft | 95.2 | | | |
| 70% Ill. Fire Clay ar | d 30% W | estern Ben | tonite Ble | nd |
| Total Clay Addition, % | 11.85 | | | |
| hrinkage, in./in. | 0.0360 | | | |
| I.F.S. Clay, % | 11.0 | | | |
| Density, lb/cu ft | 99.3 | | | |
| 50% Ill. Fire Clay a | nd 50% W | Vestern Be | ntonite Bl | end |
| Total Clay Addition, % | 8.6 | | | |
| Shrinkage, in./in. | 0.0332 | | | |
| A.F.S. Clav, % | 9.3 | | | |

97.6

Density, lb/cu ft

TABLE 8—SHRINKAGE VALUES FOR MOLDING SAND MIX-TURES, FREE EXPANSION, CONSTANT DENSITY RAMMING METHOD, 2500 F

| Pul | verized Fire | Clay-Ohi | 0 | |
|-----------------------|----------------|------------|-----------|--------|
| Clay Additions, % | 5.6 | 10.6 | 15.6 | 20.6 |
| Shrinkage, in./in. | 0.0267 | 0.0597 | 0.0489 | 0.0930 |
| A.F.S. Clay, % | 6.3 | 10.7 | 15.2 | 19.6 |
| Density, lb/cu ft | 102.0 | 102.0 | 102.0 | 102.0 |
| | Pulverized III | ite Clay | | |
| Clay Additions, % | 4.9 | 7.9 | 10.9 | 13.9 |
| Shrinkage, in./in. | 0.0439 | 0.0577 | 0.0469 | 0.067 |
| A.F.S. Clay, % | 5.6 | 8.1 | 10.7 | 14.0 |
| Density, lb/cu ft | 102.0 | 102.0 | 102.0 | 102.0 |
| Pulv | erized Fire C | lay-Illino | ois | |
| Clay Additions, % | 6.0 | 13.0 | 19.0 | 27.0 |
| Shrinkage, in./in. | 0.0282 | 0.0400 | 0.0638 | 0.077 |
| A.F.S. Clay, % | 5.7 | 10.6 | 15.6 | 20.5 |
| Density, 1b/cu ft | 102.0 | 102.0 | 102.0 | 102.0 |
| | Western Ber | ntonite | | |
| Clay Additions, % | 3.8 | 6.0 | 8.8 | |
| Shrinkage, in./in. | 0.0082 | 0.0140 | 0.0170 | |
| A.F.S. Clav, % | 5.0 | 6.7 | 8.5 | |
| Density, lb/cu ft | 102.0 | 102.0 | 102.0 | |
| | Southern Be | ntonite | | |
| Clay Addition, % | 6.6 | | | |
| Shrinkage, in./in. | _ | | | |
| A.F.S. Clay, % | 6.8 | | | |
| Density, lb/cu ft | 102.0 | | | |
| 70% Fire Clay, Illin | nois and 30% | Western | Bentonite | Blend |
| Total Clay Additions, | | | | |
| Shrinkage, in./in. | 0.0355 | | | |
| A.F.S. Clay, % | 11.0 | | | |
| Density, lb/cu ft | 102.0 | | | |
| 50% Fire Clay, Illin | | Western | Bentonite | Blend |
| Total Clay Additions, | | , | | |
| Shrinkage, in./in. | 0.0267 | | | |
| A.F.S. Clay, % | 9.3 | | | |
| Density, lb/cu ft | 102.0 | | | |

molding mixture are presented in Table 7.

In addition to making free expansion tests by ramming the specimen the standard three rams, the sands were also tested by the constant density ramming method. Table 8 gives the results by this means of testing.

Discussion of Results

Throughout this study it has been shown that due to cracking and oxidation of the specimen as the dilatometer furnace is closed around it, free expansion readings are too erratic to have value. The only practical phase of the test seems to be the measured difference between the maximum expansion and contraction readings at 2500 F. This shrinkage is shown in Tables 7 and 8 for the clays studied, both at standard ramming and constant density ramming of the test piece.

A study of the data for its relationship to clay substance is shown in Fig. 2 and Fig. 3. Here the increase in sand shrinkage is shown to increase as the clay substance increases. With the fairly large number of points determined it is reasonable to conclude that shrinkage of molding sand is a function of its clay content. The fact that all five clays of different types have points which fall in the general area of one straight line indicates that clays behave similarly at elevated temperatures. This sand shrinkage, no doubt, holds some relationship to sintering value of the sand mixture for the fluxes of the clays must increase as the

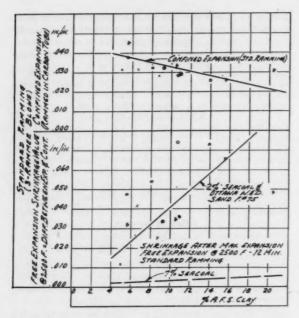


Fig. 2-Molding sand growth under confined expansion and shrinkage under free expansion due to varying clay contents when heated at 2500 F.

clay increases. The action of these upon the silica along with the dehydration of the clay would account for the measured shrinkage.

It is interesting to note that by transposing Fig. 2 and 3 the lines fall directly on one another. Thus showing sand shrinkage to be a result of heating and not effected by ramming or density. In Fig. 2, for standard ramming, the shrinkage line for a 7 per cent seacoal series is given. This shows how the slope of the shrinkage line may be varied by additions of seacoal. It would appear that with sufficient seacoal in a molding mixture the shrinkage for clay bonded sands might well be brought to zero.

Conclusions

1. Molding sands with the lowest clay substance show the lowest shrinkage on the free expansion test at 2500 F.

2. The increase of seacoal from 2 to 7 per cent reduced the shrinkage of a 20 per cent clay sand to a value below that of a 4 per cent clay sand. This offers the foundryman a means of taking full advantage of the improved density of high clay content sands and at the same time limiting the shrinkage. It appears that the advantages of using seacoal are much greater for the high clay molding sands.

PART 5

Confined Expansion Testing of Molding Sands and This Property's Relationship to Clay Substance

Having formed the conclusion that free expansion readings mean very little on sands which tend to crack and spall, it then becomes our problem to establish a more reliable means for measuring molding sand expansion. Either a stabilizing material may be added to the sand or a confined expansion method must be developed which would be suitable. Stabilizing materials effect other properties and alter the expansion readings.

In a search for a suitable confined expansion test, the authors investigated three different methods before an entirely satisfactory one was established. Trials of fused silica and mullite tubes were used with poor results before carbon tubes were tried. These have proven practical and have in addition controlled the atmosphere of the test specimen during testing. Carbon discs have been adopted for their long life and suitability.

Description of Test Method

In the method found most practical for the measurement of confined expansion, the specimen tubes are machined from a used electric furnace electrode. The dimensions of this carbon tube are: 5 in. long, 11/8 in. ID, plus 0.015 minus 0.000 in. by 15/8 in. OD. Sand is placed in the specimen tube mounted on the double end rammer pedestal and the specimen is formed by either the standard three blows or by constant density ramming method, A 11/16 in. diam by 3/8 in. thick carbon disc is placed over the specimen in the top end of the tube and a similar disc is placed on the dilatometer post with the post being the same diameter as the disc. The specimen rests upright in the furnace and is held confined in the tube, Fig. 4. The fused quartz rod and micrometer gage are assembled and the furnace closed around the specimen tube for the test. All tests of this report have been made at 2500

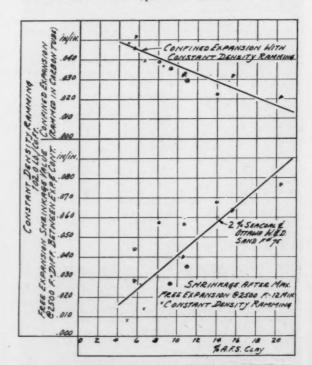


Fig. 3-Molding sand growth under confined expansion and shrinkage under free expansion, when rammed to a constant density, due to varying clay contents when heated at 2500 F.

F for 12 min heating time, with the furnace controls set for a 3 to 4-min recovery time.

Results obtained by testing confined expansion of molding sands are quite conclusive, and the authors believe this to be a practical test. First, the data obtained for the five clays under study, with sands bonded to 9 to 10 psi green compressive strength will be tabulated, in Table 9.

In Tables 10 and 11 are the results of testing the five clays and combinations of two of them, over their normal foundry application ranges, by confined expansion testing. These tests were made under standard ramming and constant density ramming methods.

In addition to the confined expansion tests by standard ramming, the same sands were tested in confined expansion by using constant density ramming and results are shown in Table 11.

TABLE 9-RELATIONSHIP BETWEEN CONFINED EXPANSION AND CLAY SUBSTANCE FOR FIVE CLAYS STUDIED, STANDARD RAMMING, 2500 F

| | Pulv. Fire Clay Ohio | Pulv. Illite Clay Ill. | Pulv. Fire Clay Ill. | West. Ben- tonite | South. Ben- tonite |
|----------------------------|-------------------------------|---------------------------------|-------------------------------|-------------------------|--------------------------|
| Amount Clay Used, % | 15.6 | 10.9 | 19.0 | 6.0 | 6.6 |
| Amount of Seacoal, % | 2.0 | 2.0 | 2.0 | 2.0 | 2.0 |
| Silica Sand, F.75, % | 82.4 | 87.1 | 79.0 | 92.0 | 91.4 |
| A.F.S. Clay, % | 15.2 | 10.7 | 15.6 | 6.7 | 6.8 |
| Moisture, % Max. Conf. | 4.5 | 4.2 | 5.2 | 3.2 | 3.8 |
| Expansion Reading, in./in. | 0.0284 | 0.0285 | 0.0266 | 0.317 | 0.0304 |

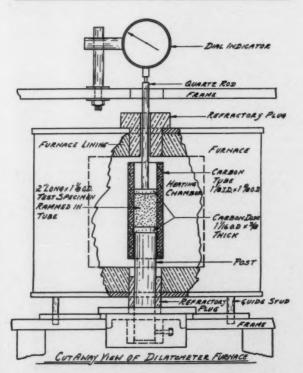


Fig. 4—Sketch showing the set up for running confined expansion tests in dilatometer by means of the carbon specimen tube.

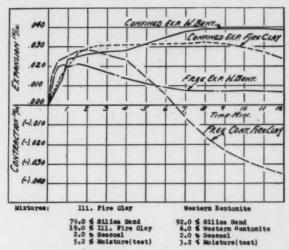


Fig. 5—Confined and free expansion of molding sand bonded to 10 psi green strength with standard ramming. Shock immersion at 2500 F.

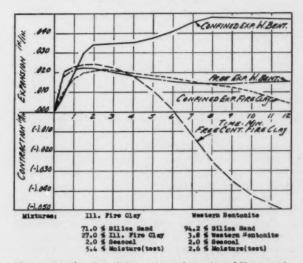


Fig. 6—Confined and free expansion of molding sands bonded to 10 psi green strength with constant density ramming. Shock immersion at 2500 F.

Discussion of Results

Confined expansion tests like free expansion tests are taken by immersing a rammed specimen in a heated furnace. The growth of the sand is taken from the dial indicator at designated time intervals throughout the 12-min heating cycle. For the most of the tests given in this report only the maximum readings are recorded in the data. This is to conserve space and is the basis of our study. It is of interest to many to see the path which the different sands follow during the heating cycle. In Fig. 5 and 6 are shown two of the clay bonded mixtures under free and confined expansion tests. Figure 5 shows the curves for the two clays when bonded to 10 psi green compressive strength and rammed to constant density. The sand expands and then shrinks or contracts. In the free expansion test this shrinkage is greater than in the confined ex-

TABLE 10-MAXIMUM CONFINED EXPANSION VALUES FOR MOLDING SANDS BY STANDARD RAMMING, 2500 F

| Pulve | rized Fire | Clay-Ohi | 0 | |
|-------------------------|--------------|------------|-----------|--------|
| Clay Additions, % | 5.6 | 10.6 | 15.6 | 20.6 |
| Conf. Exp. in./in. | 0.0498 | 0.0387 | 0.0284 | 0.0255 |
| A.F.S. Clay, % | 6.3 | 10.7 | 15.2 | 19.6 |
| Density, lb/cu ft | 101.6 | 104.6 | 108.0 | 109.5 |
| Pu | lverized Ill | ite Clay | | |
| Clay Additions, % | 4.9 | 7.9 | 10.9 | 13.9 |
| Conf. Exp. in./in. | 0.0380 | 0.0327 | 0.0285 | 0.0262 |
| A.F.S. Clay, % | 5.6 | 8.1 | 10.7 | 14.0 |
| Density, lb/cu ft | 99.0 | 99.8 | 100.5 | 101.0 |
| Pulver | ized Fire C | lay-Illino | ois | |
| Clay Addition, % | 6.0 | 13.0 | 19.0 | 27.0 |
| Conf. Exp. in./in. | 0.0457 | 0.0337 | 0.0266 | 0.0310 |
| A.F.S. Clay, % | 5.7 | 10.6 | 15.6 | 20.5 |
| Density, lb/cu ft | 100.0 | 103.7 | 108.5 | 108.5 |
| W | Vestern Ben | tonite | | |
| Clay Additions, % | 3.8 | 6.0 | 8.8 | |
| Conf. Exp. in./in. | 0.0315 | 0.0317 | 0.0285 | |
| A.F.S. Clay, % | 5.0 | 6.7 | 8.5 | |
| Density, lb/cu ft | 96.2 | 96.0 | 95.2 | |
| So | uthern Ber | ntonite | | |
| Clay Addition, % | 6.6 | | | |
| Conf. Exp. in./in. | 0.034 | | | |
| A.F.S. Clay, % | 6.8 | | | |
| Density, lb/cu ft | 95.2 | , | | |
| 70% Fire Clay, Illinois | s and 30% | Western | Bentonite | Blend |
| Total Clay Addition, % | 11.85 | | | |
| Conf. Exp. in./in. | 0.0292 | | | |
| A.F.S. Clay, % | 11.0 | | | |
| Density, lb/cu ft | 99.3 | | | |
| 50% Fire Clay, Illinois | and 50% | Western | Bentonite | Blend |
| Total Clay Additions, % | | | | |
| Conf. Exp. in./in | 0.0323 | | | |
| A.F.S. Clay, % | 9.4 | | | |
| Density, lb/cu ft | 97.6 | | | |

pansion test. The reason for the shrinkage is the dehydration of the clay and the action of the fluxes on the silica grains. The maximum expansion reading of the confined expansion test comes later in the heating cycle than for free expansion. This may be due to heat transfer through the specimen tube or the confining action of the tube itself. In Fig. 6, the sands tested are those with 10 psi green compression by constant density ramming. This gives the maximum difference covered in these tests of clay contents, and serves to illustrate the high confined expansion of a low clay sand and the high shrinkage of a high clay content sand. The data is not to infer that the clays under study have these properties for it has been shown in Fig. 2 and 3 that confined expansion and free shrinkage values depend upon the amount of the clay substance in the sand mixture, all other properties being equal, and not the type of clay.

It is shown that the confined expansion increases as the clay substance is reduced. Comparing these results of the tests by standard ramming and constant density ramming, Fig. 2 and 3, it may be seen that molding sands of less than 12 per cent clay substance have increasingly greater confined expansion as the clay is reduced, and constant density ramming is employed. This is to be expected for low clay sands show the need for increased ramming over those of the higher

clay bearing sands.

The brief study of the two combinations of clays, 70-30 and 50-50 Illinois fire clay and western bentonite served to show that confined expansion and free

shrinkage values for the clay blends fall on the same line as do other clays used alone. Thus, giving further proof that these properties are related to the quantity of clay and not dependent upon the type of clay.

The confined expansion test has proven to be a reliable means of studying a sand's expansion properties. This is due largely to the lack of sagging, cracking or spalling of the specimen while being heated in the dilatometer furnace, Fig. 4. This is considered an important development in the elevated temperature testing of molding sands. One other feature of this test, as it has been developed, is the reducing atmosphere which comes from the complete surrounding of the specimen with carbon. The specimens, as they are removed from the tubes after cooling, show no oxidation and show sand as it appears when shaken out of the mold.

The life of the carbon tubes, under normal usage, has been found to be from 12 to 20 tests before burning too thin for further use. All burning is on the outside diameter and on the lengthwise dimension. Several tubes after burning to $\frac{1}{16}$ -in. wall thickness showed no change of the inside diameter. The carbon discs have given triple the life of the specimen tubes.

Conclusions

1. Results of extensive study to develop a satisfactory method to determine thermal expansion of molding sands has resulted in adoption of the machined

TABLE 11-MAXIMUM CONFINED EXPANSION VALUES FOR MOLDING SANDS BY CONSTANT DENSITY RAMMING, 2500 F

| | 2500 | * | | |
|-------------------------|--------------|-------------|-----------|--------|
| | rized Fire | Clay-Ohi | 0 | |
| Clay Additions, % | 5.6 | 10.6 | 15.6 | 20.6 |
| Conf. Exp. in./in. | 0.0498 | 0.0322 | 0.0295 | 0.023 |
| A.F.S. Clay, % | 6.3 | 10.7 | 15.2 | 19.6 |
| Density, lb/cu ft | 102.0 | 102.0 | 102.0 | 102.0 |
| Pu | Iverized III | lite Clay | | |
| Clay Additions, % | 4.9 | 7.9 | 10.9 | 13.9 |
| Conf. Exp. in./in. | 0.0460 | 0.0390 | 0.0330 | 0.0233 |
| A.F.S. Clay, % | 5.6 | 8.1 | 10.7 | 14.0 |
| Density, lb/cu ft | 102.0 | 102.0 | 102.0 | 102.0 |
| Pulver | ized Fire C | clay-Illino | ois | |
| Clay Additions, % | 6.0 | 13.0 | 19.0 | 27.0 |
| Conf. Exp. in./in. | 0.0520 | 0.0335 | 0.0325 | 0.0214 |
| A.F.S. Clay, % | 5.7 | 10.6 | 15.6 | 20.5 |
| Density, lb/cu ft | 102.0 | 102.0 | 102.0 | 102.0 |
| W | estern Ber | tonite | | |
| Clay Additions, % | 3.8 | 6.0 | 8.8 | |
| Conf. Exp. in./in. | 0.0485 | 0.0395 | 0.0370 | |
| A.F.S. Clay, % | 5.0 | 6.7 | 8.5 | |
| Density, lb/cu ft | 102.0 | 102.0 | 102.0 | |
| So | uthern Be | ntonite | | |
| Clay Additions, % | 6.6 | | | |
| Conf. Exp. in./in. | 0.0313 | | | |
| A.F.S. Clay, % | 6.8 | | | |
| Density, lb/cu ft | 102.0 | | | |
| 70% Fire Clay, Illinoi | s and 30% | Western | Bentonite | Blend |
| Total Clay Additions, % | 11.85 | | | |
| Conf. Exp. in./in. | 0.0300 | | | |
| A.F.S. Clay, % | 11.0 | | | |
| Density, lb/cu ft | 102.0 | | | |
| 50% Fire Clay, Illinoi | s and 50% | Western | Bentonite | Blend |
| Total Clay Additions, % | 8.6 | | | |
| Conf. Exp. in./in. | 0.0357 | | | |
| A.F.S. Clay, % | 9.3 | | | |
| Density, lb/cu ft | 102.0 | | | |

carbon specimen tubes and specimen end discs. These

have proven in every test to be applicable.

2. Confined expansion testing of molding sand depends upon having the specimen rammed directly in to the tube in which it is to be tested. Carbon tubes have proven to be the least expensive and sufficiently strong for the application. In addition, the carbon produces the desired reducing atmosphere around the specimen while testing, which reproduces most mold conditions. Then too, this atmosphere prevents the burning of binders or combustible fillers which may be in the sand under test.

3. Thermal expansion properties of molding sand and their relationship to clay contents of the sand has been determined. Results of these tests serve to show that higher clay substance will reduce thermal expansion of molding sand. This, in most applications, will be a less expensive means than by other additives.

4. With the means at hand for determining expansion of molding sands under confined expansion conditions it should be possible to establish limits to control many of the common defects that are the result of the thermal growth of sand.

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DISCUSSION

Chairman: В. Н. Воотн, Carpenter Bros., Inc., Milwaukee. Co-Chairman: J. O. Ochsner, Crouse-Hinds Co., Syracuse, N. Y.

R. E. GRIM AND W. D. JOHNS, JR. (Written Discussion): 1 In the course of a study of bonding clays in molding sands in the laboratories of the Illinois Geological Survey under a project sponsored by the Illinois Clay Products Co., the compaction and flowability characteristics of sand bonded with various clays with varying amounts of tempering water have been investigated by means of a simple special piece of apparatus constructed for that purpose. Some of the results are pertinent to the conclusions pre-

sented by Schauss, Baley, and Woodliff.

The apparatus consisted of a T-shaped arrangement of cylinders 2 in. in diameter. One arm of the T was about 6 in. long, and the other arms about 1 in. in length. The short arms had removable plugs at either end. Tempered sands of a given quantity were placed in the long arm, the whole apparatus placed in a small compression machine with the long arm held vertical, and a compression force applied while the short arms were plugged. The amount of compaction was measured following the application of pressure, and after the sand had been compacted, the plug at the end of one of the short arms was removed and the amount of pressure necessary to extrude the sand out of the short arm was measured.

The following sand clay mixtures were investigated, tempered with varying amounts of water ranging from 1 to 3 per cent.

1. Illinois fireclay 10%; silica sand 90% 2. Illinois illite clay 8%; silica sand 92%

3. Western bentonite 6%; silica sand 94%
4. Southern bentonite 5%; silica sand 95%

Figure A shows a series of curves for the four sand mixtures studied, in which the height of the sand column after compression at 640 psi is plotted as a function of tempering water. In each case the same weight of sand was used. The greater the height of the sand body after compression, the less the amount of compaction of the sand, and probably also the lower the flowability.

The general appearance and relationships of the curves are quite striking, and all of them show minimum compaction at about the same moisture content, namely 2.25 per cent, except the illite clay in which minimum compaction is shown at about 1.85 per cent. It is apparent that there is a difference in com-

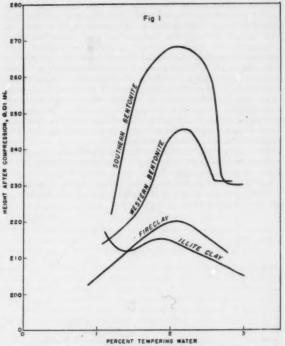


Fig. A-Curves showing the height of sand column after compression at 640 psi.

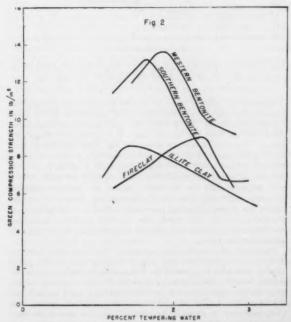


Fig. B-Curves showing green compressive strength.

paction depending on the clay bond employed. Compaction decreases in the order Illinois illite clay, Illinois fireclay, Western bentonite, Southern bentonite. Thus the illite clay compacted most easily and the Southern bentonite the least under the same conditions.

The general appearance of the curves in Fig. A are quite like the familiar curves where green compressive strength is plotted against tempering water from which it might be concluded that compaction is merely a function of green strength. Figure B gives green compressive strength curves obtained from the actual

¹ Illinois Geological Survey, Urbana, Ill.

samples studied in the compaction tests, and an analysis of Fig. A and B indicate that compaction and green strength are quite different things. Taking each sand individually, it is clear that the points of minimum compaction and maximum green strength do not coincide, and are in fact quite different. Comparison of the bentonite curves bears this out. In the moisture range from 1.0 to 1.75 per cent, the two bentonites have about the same green strength whereas the compaction is quite different. Above 1.75 per cent tempering water, the Western bentonite is the stronger of the two, yet exhibits better compaction than the Southern bentonite. In addition, at about 2.25 per cent tempering water the fireclay bonded and southern bentonite bonded sands have about the same green strength, but quite different compaction characteristics.

The sands with the higher clay content exhibit the greater compaction under the conditions of the tests. Whether or not this is a generality applicable to sands bonded with all types of clays cannot be determined without further testing. The data do indicate that the nature of the bond causes great differences in

amount of compaction.

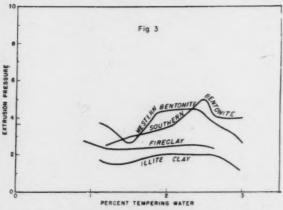


Fig. C-Curves showing pressure necessary to extrude sand 1/4 in.
after it had been compacted under 640 psi.

Figure C presents a series of curves prepared by plotting the pressure necessary to extrude sand ½ in. out of one of the short horizontal arms of the T after the sand had been compacted under 640 psi with the cap in place, as a function of tempering water. The units given are arbitrary and their relation to each other is not linear. Four units correspond to a pressure of about 640 psi, whereas two units might represent a pressure of only 200 psi. The experimental data are somewhat irregular, but it may be seen that the bentonite curves are grouped together between three and four units whereas the illite and fireclay curves are grouped around two units. That is after being compressed under the same conditions, the force necessary to extrude the illite or the fireclay.

Figure D gives a series of curves for the same sand mixtures as used above in which the vertical displacement of the piston during extrusion is plotted as a function of tempering water. Again there is a sharp contrast between the bentonite curves and those for the illite clay and the fireclay. In the sand bonded with all the different clays, the piston moves a distance greater than that necessary to extrude the sand, if all this movement were involved in pushing out the sand. In the extrusion of the sand a great deal of the movement of the piston is taken up in compacting the sand by filling pore space rather than by moving the whole sand mass. In the case of the bentonites the amount of movement involved in the compaction is much greater than that for the illite clay or the fireclay.

It seems clear from the foregoing that the compaction characteristics of sands bonded with bentonites are quite different than those bonded with an illite clay or a fireclay. Further, the data suggest that it would be more difficult to obtain uniform density of a sand under compression in an irregular body such as would exist in some molds if a bentonite were used as the bond than if a clay like the Illinois illite or fireclay were used.

E. C. Zirzow (Written Discussion): ^a However, we must bear in mind that the writers have only attempted to show the effect of various types of clay on a predetermined sand mixture. In these test results it will be noted that actually three variables are introduced into the mixture.

1. Type of clay

2. Percentage of clay

3. Percentage of moisture

Other investigators have found that any one of these variables will produce a change in density. Hence it seems to me that it would be rather difficult to determine which of these three variables was responsible for the changes in density. It would seem more logical that the changes in density could be attributed to all three of the variables.

In Part I of the paper the writers show that in order to obtain a sand mixture having a green compressive strength of 10.0 psi it is necessary to use A.F.S. clay percentages of 15.6 per cent of the Illinois fire clay ranging down to 6.7 per cent of Western bentonite. The writers note these clays as pulverized clay, Western or Southern bentonite. What is meant in terms of fineness by pulverized clay, Western and Southern bentonite? Have the writers run any tests using clay of different finenesses? Did the results compare with those reported in the paper?

In Part 1 the writers also state that the moisture contents selected were normal working or practical moisture contents. Just what method or yardstick was used to determine what these normal working or practical moisture contents should be?

In Part 1, Table 3 the writers show the effect of various additives to the base mixture on the rammed density. In other words to the base mixture containing 2 per cent seacoal an additional 7 per cent of seacoal or 0.5 per cent cereal or 1.0 per cent cereal or 1.0 per cent of woodflour is added. It will be noted that the addition of these additives does not have the same effect on density for the various types of clay. Do the writers have any explanation for this?

In Part 2 the writers suggest that constant density be used as a basis for determining the physical properties of a molding sand mixture. This suggestion is open to debate. At the present time ramming energy is held constant with the result that varying densities are obtained. The suggestion may have some merit in determining the flowability or moldability of a sand mixture. However, it would require much more data than that presented by the authors to determine what the constant density value should be for the determination of flowability or moldability.



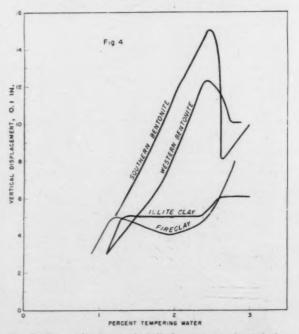


Fig. D-Curves showing vertical displacement of piston during extrusion of sand 1/4 in.

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In Part 3 the writers base their conclusions on only one method of ramming, that is the A.F.S. Standard procedure. Other investigators have found that the results obtained on the A.F.S. sand rammer closely coincide with the production of molds by squeezing or jolting. Hence in production if density were the determining factor in making molds the amount of work required to make a mold could be reduced by proper selection of molding materials.

The results obtained by the authors on confined expansion are interesting. These results corroborate the findings of other investigators that, control of confined expansion can be obtained in various ways such as varying the clay content or by adding additives such as seacoal. However, the data presented is not conclusive as to the effect of constant ramming on confined expansion at 2500 F for all clay substances, i.e., the writers note the following results.

| | SOUTHERN BENTONITE | | WESTERN | BENTONITE |
|-------------------|---------------------|---------------------|---------------------|---------------------|
| | Standard Ramming | Constant Ramming | Standard Ramming | Constant Ramming |
| Density | 96.2 | 102.0 | 96.0 | 102.0 |
| Conf. Exp. in./in | . 0.034 | 0.0313 | 0.0317 | 0.0395 |
| A.F.S. Clay, % | 6.8 | 6.8 | 6.7 | 6.7 |

In the case of Southern bentonite the confined expansion at 2500 F decreases as the density is increased while the opposite is true of the Western bentonite. There should be some explanation for these contradictory results.

The authors also confine their investigation to only one temperature, 2500 F. It would be interesting to know if the same general pattern of results exists at other temperatures.

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Mr. WOODLIFF'S REPLY

Mr. Woodliff: In order to offer a rebuttal to Mr. Zirzow's kind remarks the following is to take these in order.

The use of additives such as wood flour and cereal did not have the same effect on all types of clay-bonded sands. For that matter neither does water or increased quantity of clay substance. I believe this to be a worthwhile observation which can be tied in with practice.

Remarks in regard to constant density ramming, as stated by Mr. Zirzow, are not entirely complete. It is suggested that sand properties be determined by this method, but principally the amount of ramming energy is the desired property to measure. This is different for each type of clay and quantity of clay substance. Then too when constant density ramming is employed many of the normal properties of a bentonite-bonded sand thought to be superior become the same as for the fireclay-bonded sand, an example is green permeability. It seems that the term moldability is better applied to this measurement of rammed density energy than flowability, as suggested.

The discussion of Mr. Zirzow on the results obtained between standard ramming and constant density ramming for confined expansion of Southern and Western bentonite perhaps should be extended to include all the clays. For as the ramming energy is increased or decreased from the standard three blows the confined expansion does not necessarily increase or decrease in any relationship.

Confined expansion of sand seems to be more a function of the clay substance or type than of the rammed density, thus, a clay which has a high moisture absorption will show a lower confined expansion when rammed to a higher density. The example of this is Southern bentonite. The same is true of the Illinois fireclay, where the confined expansion was lower for the standard tests than for the constant density tests. In this instance the harder ramming, more ramming energy, was exerted in the standard testing. It is admitted that more data would be required to form a broader conclusion.

The discussion, as presented by Mr. Zirzow, is very well taken and I feel, as a co-author of the paper, will present bases for further study.

MR. ZIRZOW: There is one point made by Mr. Woodliff that I would like to discuss further. How are you going to arrive at a workable moisture content? I myself cannot "feel" clay, especially with fireclay and bentonite. If you can then you are out of the ordinary. However, if there was another figure included on workability or toughnes I probably would go along with you.

Mr. Woodliff: I think you are right. I wish I had the means of testing it, but I did not have. I go a little on my experience,

some on feel. I asked several other men to judge with me. The sand seems to feel workable. Whether we were 0.5 per cent off or more or less, I do not know. Apparently from opinions expressed, we were farther off on one than we were on the other three or four.

CHAIRMAN BOOTH: I would like to add a thought here on the "feel" for moisture content. Recently I visited a gray iron jobbing foundry in Michigan. In their sand system the sand came from the shakeout on the conveyor belt. They had a man sitting on a seat adding the moisture. He controlled a nozzle spray that sprayed water on the sand. He feels the sand and adjusts the nozzle. At the other end of the line was a boy taking samples of the sand every 15 min for checking moisture content. The man at the nozzle controlled the moisture by "feel." The moisture content test results were shown to me. There was a variation of only plus or minus 0.1 per cent. This is incredible.

MR. ZIRZOW: I question the plus or minus 0.1 per cent. However I think a man on a particular sand system in a shop can do pretty well. I was associated with a shop where they had a man tempering sand to a moisture content of plus or minus 0.3 per cent all day long. He was feeling the same sand all day long. That is the point.

CHAIRMAN BOOTH: That is a good point. When you switch from one clay bond to another there is a difference in feel.

R. G. Thorpe: The test results reported here are, I think, extremely valuable for comparison purposes, but I am puzzled by the magnitude of the values given for free or unconfined contraction. The expansion curve for any one of the various modifications of silica does not show contraction, whether it be alphaquartz, beta-quartz, tridymite or cristobalite.

We have done some work at Cornell to show that a clay, when molded into a bar, will indeed show high contraction. However, on the basis of its volume fraction in a sand mix, the clay could not possibly account for anything but a small portion of the measured contraction.

I would like to raise this question. Do the authors feel that there is a possibility that the contraction as determined by a free expansion-contraction test might be due to slumping, i.e., actual failure of the clay bonds at the test temperature they have used, 2500 F? I believe that they were operating beyond the peak strength temperature of the mixture. If you run hot compressive strength tests over the entire temperature range you will find that the strength falls off rapidly beyond about 2000 F. This is probably due to plastic flow of the clay bond phase.

The point I am trying to bring out is that we feel that the unconfined expansion-contraction test on a 2-in. vertically supported specimen is open to serious criticism because appreciable slumping may occur in the specimen due to its own weight, particularly at high temperature.

Mr. Schauss: That is a factor we did not consider in the testing we did. I think it has a lot of merit. It is definitely a factor that should have been considered.

THOMAS CURRY: 4 I would like to ask Mr. Woodliff if he expects to do any work on correlation of confined expansion tests in the carbon tube vs the open sand or free expansion test in open air.

Mr. Woodliff: We tried to get a correlation in this work but the free expansion was erratic. We could not get correlation until we adopted the confined expansion tests between clays or even of repeated tests. We were able to repeat our tests with quite a high degree of accuracy when we used confinement specimens. I believe Mr. Thorpe brought that out. While we were measuring free expansion we did get contraction which might be due to flexing or might be due to sagging. I have from observation seen many specimens come out of a furnace at an angle off from perpendicular. I believe we measured this sagging and called it shrinkage in some cases, but actually it was only sagging of the specimen.

Mr. Curry: I believe in your paper there was a figure of about 0.045 for one mix with the free expansion and 0.015 for the confined expansion. I was wondering whether the atmosphere alone had anything to do with that particular test.

MR. WOODLIFF: Certainly in the furnace atmosphere high oxidation was present, and we immediately burned seacoal and other

⁸ Research Fellow, Cornell University, Ithaca, N. Y.

⁴ Metallurgist, Lynchburg Foundry Co., Lynchburg, Va.

organic materials out of the sand.

In the case of a high clay sand we did get glazing of the specimen to some degree, where as we did not get that in the case of the lower clay content sand.

If that glazing proceeds at a rapid rate, we might get a higher expansion with a high clay content sand. In fact, we did that in several cases and obtained a higher expansion with a high clay content sand because of the glazing action which protected or built around the specimen a vitrified shell or envelope and held the specimen rigid. In the reducing atmosphere produced by the carbon tube of the confined expansion test organic binders and seacoal was not removed, it was not burned out. Therefore, they were taking their normal reaction as they do in the molds. At least we thought that.

At least we thought that.

Mr. Schauss: This confined expansion test with the graphite tube offers more promise than most foundrymen realize. From a practical application, possibly the elimination of defects or expansion defects in the foundry, we have been able to correlate the expansion before and after. That is the important part of the whole thing. We have been able to take sands out of a given foundry and test them. Sands that, on certain heavy work, were giving scabbing troubles were tested to determine that expansion with the carbon tube. Clay content was increased and those sands applied to the same type of work. We were able to eliminate these expansion defects in the foundries. At the same time, repeating the test again, we found that we had markedly reduced the confined expansion. It worked out in practice just as well as in theory, and that is the important phase of it as far as our picture is concerned. We do like it as a means of possible control.

BRUCE BOECKER: 8 As you go to higher clay contents, you do

need more moisture and you also lower permeability. When you run your clay content up to 18 or 19 per cent you will have to have quite a bit of moisture. The high clay sand will absorb much more moisture than will a low clay sand. Would the high moisture in a high clay sand cause trouble? Will that alter your reasoning in some cases?

Mr. Schauss: With a constant density, or within any given density of sand, your permeability will be relatively constant. You will get a little change in your permeability. The moisture content, as long as it is tied up with a clay substance, is not going to get anybody into difficulty. When you have excess moisture above the best workable amount, I think you can expect some difficulty, but when you have your moisture at a level where it is going to be tied up essentially with the clay particles I do not think you are going to get into too much difficulty. That is an observation over a period of time.

Mr. Zirzow: I have a question on combined free expansion. You selected 2500 F as a temperature for running these tests. Have you tried any other temperatures, and why did you select 2500 F? To me it sounds a little high for gray iron. It might be all right for steel. You are probably starting some incipient fusing or glazing of the clay itself.

Mr. Schauss: We had many temperatures involved in this when we started out. It was difficult to include everything that we liked to have found out. We have a book full of data that we have to work on. There is a lot more to be done in this direction. Your temperature question is a good one and merits attention.

⁸ Sand Technician, The Minster Machine Co., Minster, Ohio,

ON PHYSICAL PROPERTIES AND SOLIDIFICATION CHARACTERISTICS OF TIN BRONZES

By

Bernard N. Ames¹ and Noah A. Kahn¹

ABSTRACT

The authors demonstrate effect of superheating and casting temperatures with and without phosphor-copper deoxidation on tensile strength, ductility, macrostructure, microstructure, density and pressure tightness of Navy gun metal. The results indicated that when proper melting atmospheres are employed, degree of superheat or tapping temperature is not critical. Casting temperatures, however, were shown to have a profound influence on physical properties and pyrometric control of production casting operations is strongly recommended and desirable.

For the design of test bar utilized, optimum properties occurred prior to the macro transformation from columnar and equiaxed crystallization to a completely equiaxed structure and prior to the micro transformation from the regular geometric dendrite pattern to the restricted and random dendrite confinential

The authors suggest that optimum mechanical properties of tin bronzes are associated with a critical velocity of crystallization. Utilization of test bars as a measure of melt quality is also discussed. The authors also show that density of gun metal bronze increases with decreasing pouring temperature and that pressure tightness and the ultimate in physical properties are not necessarily synonymous with high density values.

Generally, it is stated, a fine equiaxed grain structure precipitated by low pouring temperatures will not yield the ultimate in tensile, elongation and pressure tightness. This is due in a large measure to differences in the mode of solidification between castings poured at high and low pouring temperatures, which they attempt to explain, and the resultant differences in the volume of voids created. Effects of phosphor-copper deoxidation on many physical properties are also illustrated.

Introduction

TIN BRONZES ARE SUSCEPTIBLE to an interdendritic type of microshrinkage often resulting in defective castings under hydrostatic pressure test, particularly if the benefits derived from the as-cast skin are removed by machining. It is generally believed that this condition is due, in part, to the long solidification range of this class of alloys and its mode of solidifica-

tion, which makes adequate feeding of molten metal to the interdendritic voids quite difficult. Of course, this condition is further aggravated by higher gas contents in the alloy.

The Navy Department, as a user of large tonnages of tin bronze castings has been keenly interested in a basic study and in a more thorough understanding of the solidification characteristics of this class of alloys and has sponsored a project at the Material Laboratory, N. Y. Naval Shipyard to investigate this phenomena and to develop methods and techniques for its control. This paper reports on one phase of the work being conducted under this project.

No unanimity of opinion appears to exist among nonferrous foundry operators as to the effect of superheating and pouring temperatures on mechanical properties, density, and hydrostatic pressure resistance of nonferrous castings. In some cases foundry operators attempt to achieve increased pressure tightness of tin bronze castings by lowering the casting temperatures as much as practicable. This increased resistance to hydrostatic pressure has been attributed to the grain refinement achieved and has been popularly associated with improved mechanical properties. On the other hand, many operators prefer to tap and pour at higher temperatures and they make claims for improved fractures and increased resistance to hydrostatic pressures. The objective of this investigation is to provide additional experimental data to the large volume of published material already on hand which might aid in resolving some of these issues i.e., effect of superheating temperatures, casting temperatures, and phosphorcopper deoxidation on some mechanical properties, macro grain size and shape, microstructure, density, hydrostatic pressure resistance and solidification characteristics of Navy gun metal.

Related Work By Other Investigators

Summaries and excerpts from published works of some investigators which pertain to the problems investigated by the authors are discussed in the following paragraphs. Hensel, Tamman and Schwartz discussed the theoretical concepts of solidification. Karr and Rawdon, Carpenter and Elam, Primrose and

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Thews7 all showed that casting temperatures has through its influence on rate of solidification a greater effect on soundness than any other factor. The work by Cowan8 was significant in illustrating that minute shrinkage cavities in certain heterogeneous alloys are due to shrinkage accompanying the change in volume, between liquid and solid phases, of a component of relatively low freezing point where it last freezes after the bulk of the alloy has solidified at higher temperatures. Shortly afterward Dews^{9,10} emphasized the importance of volume change on freezing and of the freezing range itself in explanation of many bronze foundry defects. Unfortunately Dews' work at that time did not seem to breach the gap between metallurgist and foundrymen, particularly in this country, and many years of intensive study and discussion on gases and melting problems overshadowed and neglected the even more important problem, perhaps, of the solidification characteristics of this type of alloy. Some excerpts from Dews' work are summarized be-

(a) "In the average foundry the effect of the casting temperature will be of greater significance than these other factors since the thermal properties of the molds and metal may necessarily be invariable."

(b) "The casting and cooling conditions, particularly in devising specifications or control tests, should not be lost sight of."(c) "There can be little doubt that the casting temperature

(c) "There can be little doubt that the casting temperature is the most important factor requiring control in the bronze foundry. The effect of most other variables is dwarfed besides that of casting temperature, and it may be said that success or failure in producing good castings depends ultimately on casting temperature control."

(d) "It also follows that the optimum temperature for a certain type of test bar will not necessarily give the best results with any type of casting. This is a most important matter from

the practical founder's point of view.

(e) "In each case abnormally high density values occur with low casting temperatures and not at casting temperatures which

are associated with the highest strength.'

(f) "The truth is that a discolored fracture indicates that the bronze has not been cast at its proper temperature, and without any other alterations either in the method of melting or alloying the same bronze can be cast with a normal fracture when its casting temperature is chosen more appropriately."

Density of Castings

In 1939, Rahm¹¹ stated that the density of castings obtained is an important factor but not the only one. He indicated that a high strength is always associated with high density but the converse is not always true. For this reason he claims, no uniformly good castings can be expected unless the other factors involved are clearly understood and brought under control. Rahm stated that grain size had little if any effect on the characteristics of cast metal and that slightly higher values of tensile strength and ductility could be expected for the larger grains.

Pearson and Baker¹² in 1941 again re-emphasized that the bronzes are prone to dispersed shrinkage unsoundness by reason of their long freezing range and that this unsoundness tends to take a harmful form namely, interdendritic fissures. They pointed out that these fissures have comparatively little effect on density but are detrimental to mechanical properties. They state that in sand castings where the rate of heat abstraction is much lower, feeding is seriously hindered by the growth of dendrites in the solidifying

zone and considerable shrinkage unsoundness is almost inevitable.

Hesse¹³ in studying the causes for porosity in nonferrous castings pointed out that intercrystalline shrinkage porosity may be controlled by the rate of solidification which points to the importance of casting temperatures. He noted however that this factor is a natural phenomenon which cannot be easily controlled and presents a considerable handicap in the manufacture of pressure tight castings. He stated that to obtain maximum soundness and maximum physical properties a casting has to be poured within a narrow temperature range.

Baker, Child and Glaisher,¹⁴ in 1944, stated that shrinkage porosity although possibly small in volume is detrimental to the pressure tightness and strength of the casting. In some cases they claim a suitable amount of gas serves to disperse porosity more uniformly through the casting with the result that pressure tightness and mechanical strength are consider-

ably improved.

Test Bar Designs

The year 1945 may be termed the start of the "test bar era" when intensive work was done by George, 15 Baker, 16 Eastwood and Kura, 17 and others on the sensitivity of test bars in determining metal quality. Basic information pertaining to solidification characteristics of tin bronzes were derived from these studies.

George¹⁵ discussed the effects of furnace atmospheres, superheating temperature, pouring temperature and deoxidation practice using phosphor-copper on the causes of test bar failures in Navy "G" and "M" metal.

Eastwood and Kura¹⁷ conducted an intensive survey on the sensitivity of various test bar designs as a measure of melt quality, utilizing the 85-5-5-5 alloy, and on the effect of pouring temperatures and mold materials on the physical properties and lead distribution of this alloy.

Pell-Walpole¹⁸ determined that the maximum soundness and highest and most uniform properties throughout an ingot section are obtained in degassed, chill-cast bronzes, provided the metal is poured at the minimum rate which will fill the mold without cold shuts. The formula developed assumes that pouring temperature, mold temperature, and mold material are constant. He stated that variations of these factors within practical limits has little effect on soundness as compared with the effect of pouring rate, though they affect surface quality and mechanical properties, the latter by causing variations in the amount and mode of distribution of the compound phases.

Baker¹⁹ in studying pressure tightness and porosity control in bronze and castings stated that for simple casting designs, where section thicknesses are relatively uniform, and where substantially complete feeding is practicable and economic, the pouring temperature and phosphorus content of the alloy should be kept at the lowest possible level compatible with other considerations. He claims a beneficial effect for small residual gas contents in effecting a uniform distribution of porosity in the test casting.

Pell-Walpole²⁰ stated that the porosity of a casting

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may be determined by the comparison of an experimentally determined value of its density with some chosen standard value of density for the same composition. He claimed that within the limits of normal experimental error it may be considered that the maximum cast density of tin bronzes deoxidized and containing 0.02 to 0.03 per cent residual phosphorus does not vary appreciably either with tin content or with structural variations; the maximum density of the alpha solid solution varies from 8.87 to 8.89 and that of the delta is 8.90, so that any mixture of the phases likely to occur in normal bronze compositions has a maximum density of 8.88 ± 0.01 gm per cc. He states that the presence of phosphorus in bronzes in amounts more than that required for deoxidation (0.02 to 0.03 per cent) causes a severe and almost linear decrease in the maximum cast density. This decrease is a genuine constitutional effect caused by the low density of the Cu₃P phase though low density of high phosphorus bronzes has frequently been attributed incorrectly to the effects of phosphorus on porosity. In discussing microshrinkage, he states that these fissures occupy little volume hence they have only a slight effect on density, but they are severely detrimental to mechanical strength and are the chief causes of leakage in hydraulic castings. The author specifically does not suggest utilization of density measurements alone as a guide to pressure tightness.

Mechanism of Solidification

Finch²¹ and Frear²² both discussed the mechanism of solidification of tin bronzes. The former discussed the mode of crystallization and the effect of section size on the temperature gradient and mushy zone and the latter suggested that the tendency of the tin bronzes to form large dendrites and the resultant difficulty in feeding the interdendritic cavities formed is chiefly responsible for the shrinkage porosity and the "leakers" experienced with this class of alloys. He suggests that this problem can be minimized by pouring at a low temperature, thus decreasing the solidification time and dendrite size.

Smith²⁸ recently discussed problems relating to the mechanics of solidification in which he outlined previous work by other investigators and among other things pointed out that a casting showing considerable equiaxed structure adjacent to the feeding head will be fed only with difficulty, regardless of the inherent ability of the head to feed. He indicated that in heavy sections the effect of temperature gradient is small and an equiaxed structure is to be expected. Such castings, he claims, are difficult to cast soundly and the normal procedure is to cast as cold as possible in order to keep contraction to a minimum. With such practice, little feeding takes place from risers, the risers functioning solely as heat reservoirs.

Kondic²⁴ in a recent article discusses the elements required for a test bar design which will be sensitive to metal quality. He points out that the sensitivity of the test bar to metal quality should be available over a range of casting conditions and as Eastwood and Kura also noted, the condition of optimum properties and optimum sensitivity to metal gas content are contradictory. He states that the mechanical properties

of the test bar can be related satisfactorily to the structural variations (grain size and distribution of constituents) and variations in the type and amount of unsoundness.

Experimental Procedure

In the course of this investigation the following variables were studied under closely controlled plant conditions:

- Variations in the type of melting unit (Indirectarc and open-flame oil-fired units)
- Variations in tapping temperature (degree of superheat)
- 3. Variations in casting temperature
- Variations in use of deoxidizer (2 oz P-Cu/100 lb and no P-Cu deoxdiation).

The effect of varying each of the above elements, singly, was noted on the following characteristics of the gun metal castings produced:

- Mechanical properties (tensile strength, elongation)
- 2. Macrostructure
- 3. Microstructure (as revealed by metallography and microradiography)
- 4. Density
- 5. Hydrostatic pressure resistance
- 6. Solidification characteristics.

For determination of variations in the ultimate strength, elongation, macrostructure and microstructure due to variations in the type of melting unit, tapping temperature, casting temperature and deoxidation practice, 16 heats were run under normal operating plant conditions. All charges were of commercial gun metal ingot previously certified for quality. Eight of these heats were melted in an indirect arc furnace (nominally 500-lb cold charge capacity). Five of the eight heats melted in the indirect arc furnace were superheated to 2500, 2400, 2300, 2200 and 2100 F respectively and deoxidized with 2 oz phos-copper per 100 lb of molten metal. Three test bars were poured at each casting temperature at 50 F decrements as the metal cooled. The other three heats were superheated to temperatures of 2400, 2200 and 2100 F with no deoxidation. The same number of test bars, at the same casting temperature intervals were poured.

The other eight heats were then melted in an 800-lb capacity open-flame, oil-fired furnace under the same test conditions noted above, i.e. five were superheated to 2500, 2400, 2300, 2200 and 2100 F respectively and deoxidized with 2 oz P-Cu per 100 lb of molten metal and three were superheated to 2400, 2200 and 2100 F with no deoxidation. Three test bars were poured at each casting temperature at 50 F decrements as the metal cooled.

As indicated above, triplicate test bars were poured from each casting temperature decrement in baked, oil-sand blackened cores of a modified Navy Dept. Fig. 10 design.* Of the three test bars cast at each temperature interval, two were machined and ultimate strength

^{• &}quot;General Specifications for Inspection of Material," Appendix II, Metals Part A, Definitions and Tests, Navy Dept., 1 April 1947.

and elongation values were obtained. The third test bar in all cases was sectioned longitudinally approximately on the center-line of the gage section, polished and etched in 30 per cent nitric acid for macro examination. Transverse sections were taken through the center of the gage section for metallographic examination on the same half of the test bar utilized for the macro examination.

Microradiograph studies were performed on transverse sections of the third test bar and whenever possible specimens were machined to an average thickness of 0.003 in. with parallel faces. Exposures were made on EK spectrographic plates No. 548. Copper radiation was utilized 35 kv, 20 ma. The average exposure time was 45 sec at a 53/4-in. film to source distance.

Chemical analyses were made from chips taken from the coupons of the test bars utilized for the tensile and elongation determinations. A chemical analysis was

made from every ladle of each heat.

A detailed log is shown in Table 1, tabulating the heat number, specimen number, type of furnace utilized, deoxidation conditions, degree of superheat, casting temperature, yield point, tensile strength, elongation and chemical analyses for each ladle of the 16 heats melted.

For the determination of density and hydrostatic pressure resistance as influenced by the type of melting unit, tapping temperature, casting temperature and deoxidation practice, flanged bushings gated as shown in Fig. 1 were cast. A flanged bushing was chosen since it provided a change in section and would co:relate more closely perhaps with actual production casting conditions. In all, 20 heats of 300- to 700-lb capacity were run. Five were melted in the indirect arc furnace, superheated to 2500, 2400, 2300, 2200, and 2100 F, respectively and deoxidized with 2 oz P-Cu per 100 lb of molten metal and five in the same melting unit, under the same superheat conditions, with no deoxidation. The same conditions were duplicated in the open-flame, oil-fired furnace. Two bushings were poured at each casting temperature interval as the metal cooled. Casting temperatures of 2300, 2200, 2100, 2050 and 1950 F were utilized depending of course on the tapping temperature involved. The as-cast dimensions of the flanged bushings are shown in Fig. 2. These cylinders were machined

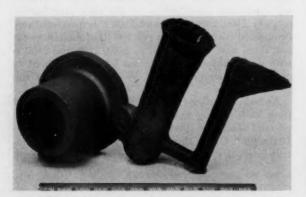


Fig. 1—Hydraulic pressure test cylinder casting showing gate and riser.

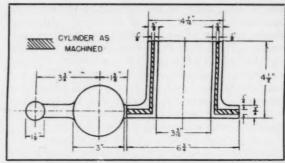


Fig. 2—Hydraulic pressure test cylinder casting illustrating gating and machining.

equally on the OD, ID and flange so that the wall and flange thicknesses were ½ in. All cylinders were then pressure tested in the rig shown in Fig. 3 at 50 lb, 100, 200, 300, 400 and 500 lb, and held at each pressure for 5 min. The test was discontinued upon failure of the bushing at any test pressure. The bushings were then remachined to a ½ in. wall thickness removing equal amounts of metal from the OD and ID and retested as above.

Density determinations were obtained on the complete cylinders by the water displacement method. In all cases, chemical analyses were obtained for every ladle of every heat. Separately cast test bars for each heat indicated that the physical properties were well above specification requirements. The chemical analyses obtained are tabulated in Table 2, 3, for each furnace and deoxidation condition.

Effect on Mechanical Properties

The graph in Fig. 4 illustrates the effect of tapping and pouring temperatures on the average tensile and elongation values obtained from the test bars poured from gun metal melted in the open-flame, oil-fired furnace and deoxidized with 2 oz P-Cu per 100 lb of molten metal. Each solid line of the five in both the ultimate strength and elongation parts of the graph

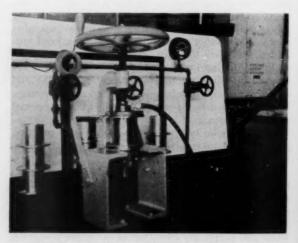


Fig. 3—Hydraulic pressure testing rig showing cylinder in place for testing.



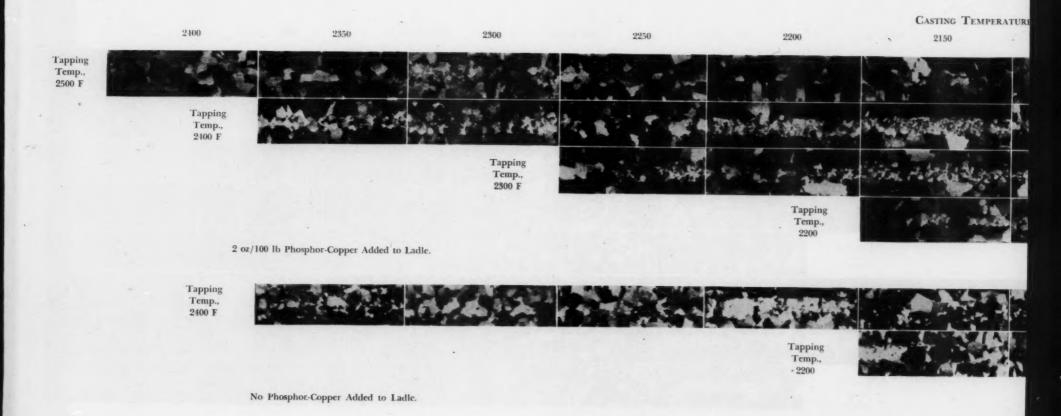
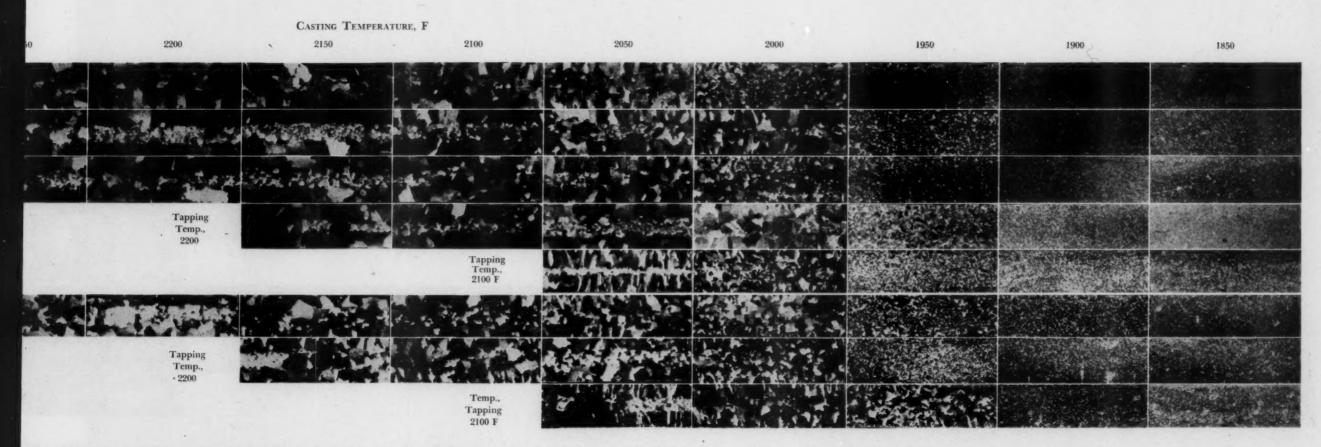


Fig. 8-Effect of Tapping and Casting Temperatures on the Macrostructure of Longitudinal Sections of Gun Metal Test



ON THE MACROSTRUCTURE OF LONGITUDINAL SECTIONS OF GUN METAL TEST BARS. MAGNIFICATION: X 1. ETCHANT: 30% HNO3. FURNACE: OIL-FIRED, OPEN-FLAME.

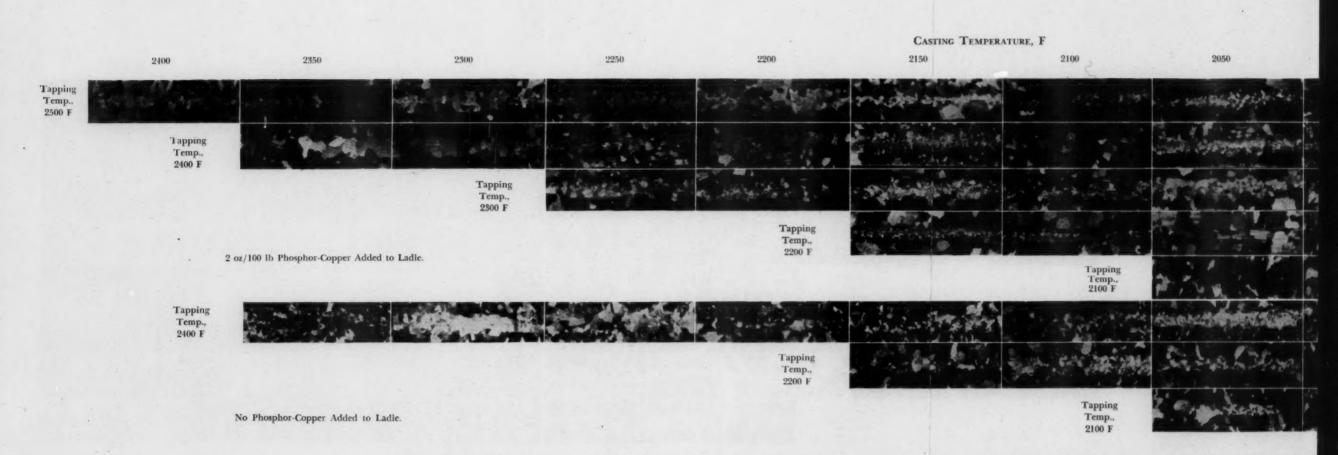
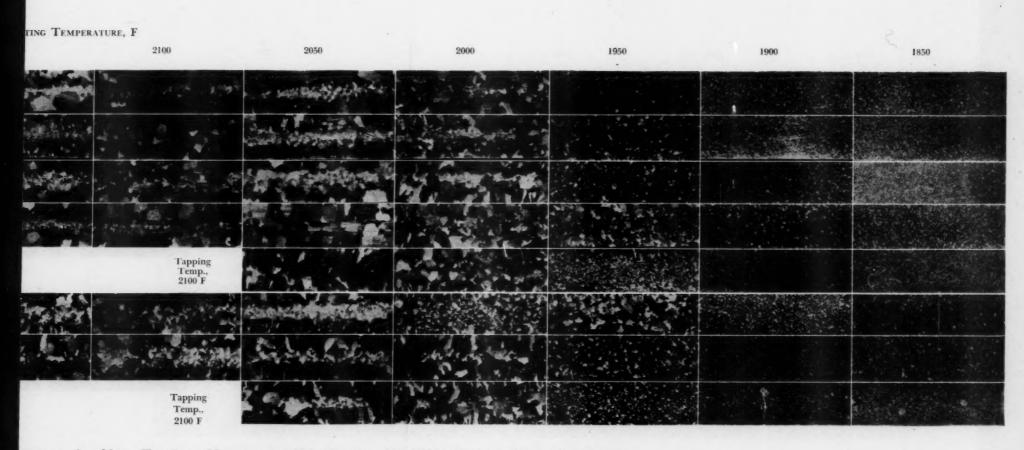


FIG. 9-EFFECT OF TAPPING AND CASTING TEMPERATURES ON THE MACROSTRUCTURE OF LONGITUDINAL SECTIONS OF GUN METAL TEST BARS. MAGNIFICATION: X 1. ETCHANT: 30%



CTIONS OF GUN METAL TEST BARS. MAGNIFICATION: X 1. ETCHANT: 30% HNO3. FURNACE: ELECTRIC ARC ROCKING.

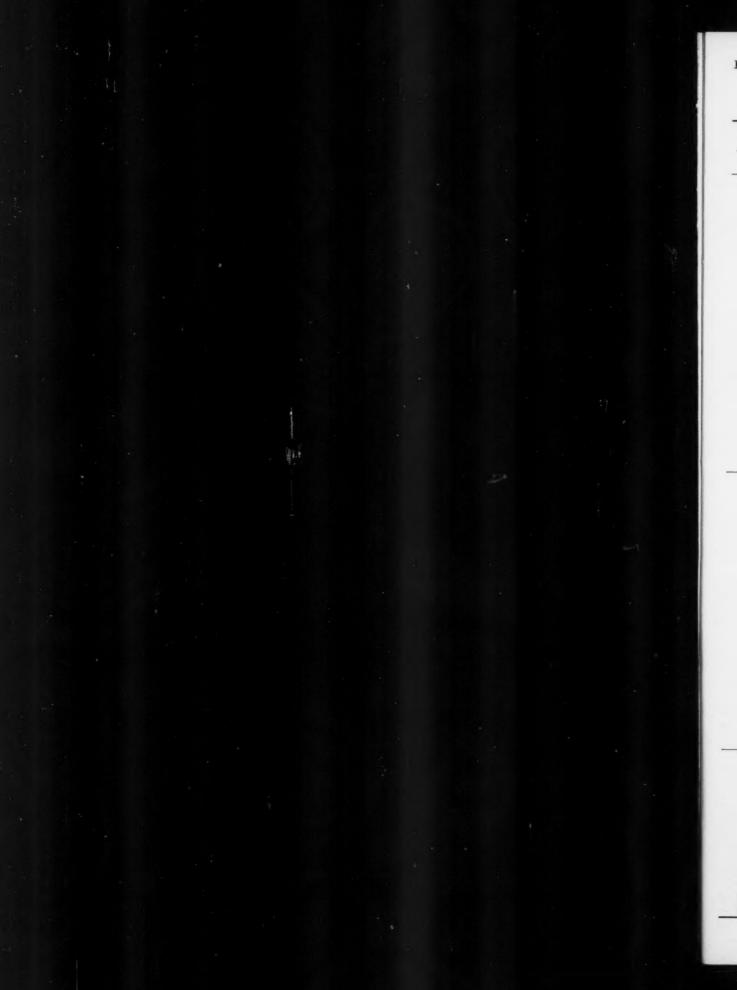


Table 1-Physical and Chemical Properties of Gun Metal as Affected by Melting Unit, Tapping and Casting Temperatures and Deoxidation

| | | Physical Properties Tensile | | | | | Chemical Analyses, Per Cent | | | | |
|----------|----------|--------------------------------|----------------|----------------------------------|-------|------|-----------------------------|--------|------|------|-----|
| Test Bar | Casting | | Strength, | Elongation, | | | | | 1 | | |
| No. | Temp. °F | psi | psi | % in 2 in. | Cu | Sn | Zn | Pb | Ni | Fe | |
| | | Н | | , Indirect-Arc le Addition: P | | | | 2500 F | | | |
| A2 | 2400 | 15750 | 23750 | 12.0 | 87.50 | 8.26 | 3.53 | 0.20 | 0.49 | 0.01 | 0 |
| A3 | 2242 | 15220 | 27990 | 17.0 | 07.40 | | | | | | |
| B2 | 2350 | 18750 | 33750 | 20.5 | 87.50 | 8.26 | 3.53 | 0.20 | 0.49 | 0.01 | (|
| B3 C2 | 2300 | 19500 19250 | 39500 40500 | 38.0 33.0 | 87.50 | 8.26 | 3.53 | 0.20 | 0.49 | 0.01 | (|
| C3 | 2300 | 20750 | 41250 | 28.5 | 07.30 | 0.40 | 3.33 | 0.20 | 0.19 | 0.01 | , |
| D2 | 2250 | 21000 | 38750 | 26.0 | 87.50 | 8.26 | 3.53 | 0.20 | 0.49 | 0.01 | - |
| D3 | | 19500 | 42000 | 35.0 | | | | | | | |
| E2 | 2200 | 21250 | 44000 | 38.5 | 87.50 | 8.26 | 3.53 | 0.20 | 0.49 | 0.01 | 1 |
| E3 | | 21250 | 46250 | 46.0 | | | | | | | |
| F2 | 2150 | 21000 | 46000 | 45.0 | 87.91 | 8.35 | 2.92 | 0.21 | 0.58 | 0.02 | |
| F3 | 2100 | 21000 | 42500 | 31.0 | 07.01 | 0.95 | 0.00 | 0.01 | 0.50 | 0.00 | |
| G2 G3 | 2100 | 21250 21360 | 46000 44720 | 41.0 35.5 | 87.91 | 8.35 | 2.92 | 0.21 | 0.58 | 0.02 | |
| H2 | 2050 | 21610 | 47490 | 30.0 | 87.91 | 8.35 | 2.92 | 0.21 | 0.58 | 0.02 | |
| H3 | 4000 | 22000 | 48500 | 50.0 | 01.01 | 0.00 | | 0.4 | 0.50 | 0.00 | |
| 12 | 2000 | 22000 | 47000 | 40.0 | 87.91 | 8.35 | 2.92 | 0.21 | 0.58 | 0.02 | |
| 13 | | 21500 | 47250 | 49.0 | | | | | | | |
| J2 | 1950 | 23750 | 48500 | 31.5 | 87.91 | 8.35 | 2.92 | 0.21 | 0.58 | 0.02 | |
| J3 | | 23750 | 49000 | 35.5 | | | | | | | |
| K2 | 1900 | 25000 | 44000 | 18.5 | 87.22 | 8.60 | 3.33 | 0.22 | 0.60 | 0.02 | |
| K3 | 1010 | 25000 | 45750 | 21.5 | 00 90 | 0.00 | 9 99 | 0.00 | 0.00 | 0.00 | |
| L2 L3 | . 1850 | 25130 26140 | 41460 44030 | 14.5 17.5 | 87.22 | 8.60 | 3.33 | 0.22 | 0.60 | 0.02 | |
| 20 | | 40110 | 11000 | 17.0 | | | | | | | |
| 4.0 | 9950 | | | le Addition: P 33.0 | | | | | 0.69 | 0.01 | |
| A2 A3 | 2350 | 19500 20000 | 40500 | 30.0 | 87.38 | 8.08 | 3.81 | 0.03 | 0.68 | 0.01 | |
| B2 | 2300 | 20600 | 41450 | 33.0 | 87.38 | 8.08 | 3.81 | 0.03 | 0.68 | 0.01 | |
| B3 | 2000 | 20100 | 44720 | 42.0 | 07.50 | 0.00 | 0.01 | 0.00 | 0.00 | 0.01 | |
| C2 | 2250 | 21100 | 45220 | 44.0 | 87.38 | 8.08 | 3.81 | 0.03 | 0.68 | 0.01 | |
| C3 | | 21360 | 42710 | 35.5 | | | | | | | |
| D2 | 2200 | 21000 | 47000 | 54.0 | 87.38 | 8.08 | 3.81 | 0.03 | 0.68 | 0.01 | |
| D3 | | 21360 | 44720 | 34.0 | | | | | | | |
| E2 | 2150 | 21600 | 46480 | 50.5 | 87.38 | 8.08 | 3.81 | 0.03 | 0.68 | 0.01 | |
| E3 | 9100 | 22610 | 47480 | 53.0 | 05 95 | 0.1# | 9 2 2 | 0.00 | 0.66 | 0.01 | |
| F2 F3 | 2100 | 21360 22250 | 47730 48500 | 48.0 49.5 | 87.37 | 8.17 | 3.55 | 0.23 | 0.66 | 0.01 | |
| G2 | 2050 | 21360 | 49740 | 60.5 | 87.37 | 8.17 | 3.55 | 0.23 | 0.66 | 0.01 | |
| G3 | 2030 | 21360 | 49490 | 52.5 | 07.07 | 0.47 | 0.00 | 0.20 | 0.00 | 0.01 | |
| H2 | 2000 | 21360 | 47730 | 42.0 | 87.37 | 8.17 | 3.55 | 0.23 | 0.66 | 0.01 | |
| H3 | | 22610 | 50750 | 62.0 | | | | | | | |
| 12 | 1950 | 23860 | 50750 | 40.0 | 87.37 | 8.17 | 3.55 | 0.23 | 0.66 | 0.01 | |
| 13 | | 24370 | 50750 | 40.0 | 0.00 | 0.5 | | 0.00 | 0.00 | 0.00 | |
| J2 | 1900 | 25000 | 46000 | 22.0 | 87.37 | 8.17 | 3.55 | 0.23 | 0.66 | 0.01 | |
| J3 | 1050 | 24370 25870 | 51250 | 34.0 | 97 10 | 9 97 | 3.57 | 0.18 | 0.68 | 0.01 | |
| K2 K3 | 1850 | 24120 | 42710 42960 | 15.0 17.0 | 87.18 | 8.37 | 3.31 | 0.10 | 0.06 | 0.01 | |
| N.J | | 41140 | 14300 | 17.0 | | | | | | | |
| | | Н | | , Indirect-Arc le Addition: P | | | | 2300 F | | | |
| A2 | 2250 | 20850 | 43720 | 33.5 | 87.22 | 8.48 | 3.57 | 0.20 | 0.50 | 0.02 | - |
| A3 | | 20600 | 44470 | 38.0 | | | | | | | |
| B2 | 2200 | 21250 | 44500 | 37.5 | 87.22 | 8.48 | 3.57 | 0.20 | 0.50 | 0.02 | - 1 |
| B3 | | 20750 | 44000 | 38.0 | 00.00 | 0.10 | | 0.00 | 0.85 | 0.00 | |
| C2 | 2150 | 21000 | 46000 | 41.0 | 87.22 | 8.48 | 3.57 | 0.20 | 0.50 | 0.02 | |
| C3 | 0100 | 20850 | 45730 | 43.5 | 97 99 | 0.40 | 9 87 | 0.00 | 0.50 | 0.00 | |
| D2 | 2100 | 21600 | 45980 | 37.5 53.0 | 87.22 | 8.48 | 3.57 | 0.20 | 0.50 | 0.02 |) |
| D3 E2 | 2050 | 21600 22110 | 48250 48990 | 53.0 44.5 | 87.01 | 8.61 | 3.64 | 0.19 | 0.52 | 0.02 | |
| A.com | 2000 | 21250 | 48250 | 49.0 | 07.01 | 0.01 | 3.01 | 0.13 | 0.04 | 0.02 | |
| F3 | | mamutu. | 20400 | | | | | | | | |
| E3 F2 | 2000 | 23120 | 49500 | 43.5 | 87.01 | 8.61 | 3.64 | 0.19 | 0.52 | 0.02 | 1 |

Table 1 (cont.)—Physical and Chemical Properties of Gun Metal as Affected by Melting Unit, Tapping and Casting Temperatures and Deoxidation

| | | Phys | ical Proper Tensile | ties | | | Chem | ical Analys | es, Per Cen | t | |
|--|--|---|--|--|---|--|---|--|--|--|---|
| Test Bar No. | Casting Temp. °F | Yield Point, psi | Strength, psi | Elongation, % in 2 in. | Cu | Sn | Zn | Pb | Ni | Fe | |
| 7 | | H | | , Indirect-Arc | | | | | | | - |
| G2 | 1950 | 24000 | 48500 | 32.0 | 87.01 | 8.61 | 3.64 | 0.19 | 0.52 | 0.02 | 0 |
| G3 H2 | 1900 | 23870 26380 | 48990 46230 | 35.0 23.0 | 87.01 | 8.61 | 3.64 | 0.19 | 0.52 | 0.02 | 0 |
| H3 | | 25250 | 44950 | 19.0 | | | | | | | |
| 12 13 | 1850 | 24620 24020 | 43970 44720 | 18.5 19.0 | 87.25 | 8.67 | 3.35 | 0.20 | 0.50 | 0.02 | 1 |
| 13 | | | | , Indirect-Arc | Flectric F | urnace Tan | ning Temp | 9900 F | | | |
| | | | | Addition: P | | | | . 2200 1 | | | |
| A2 | 2150 | 21000 | 43250 | 36.0 | 87.54 | 8.58 | 3.26 | 0.10 | 0.48 | 0.02 | 0 |
| A3 | 0100 | 21200 | 43720 | 38.0 | 07 14 | 0 =0 | 9.00 | 0.10 | 0.40 | 0.00 | , |
| B2 | 2100 | 22000 | 46250 | 39.0 | 87.54 | 8.58 | 3.26 | 0.10 | 0.48 | 0.02 | (|
| B3 | 9050 | 20500 | 47000 | 44.0 | 97 07 | m cm | 4.10 | 0.10 | 0.00 | 0.00 | |
| X2* X3* | 2050 | 20000 20610 | 46500 46750 | 40.5 47.5 | 87.07 | 7.67 | 4.12 | 0.18 | 0.93 | 0.02 | (|
| | 2000 | 23620 | 48740 | 38.0 | 87.54 | 8.58 | 9.96 | 0.10 | 0.49 | 0.00 | (|
| C2 C3 | 2000 | 21610 | 48490 | 39.5 | 07.34 | 0.36 | 3.26 | 0.10 | 0.48 | 0.02 | |
| D2 | 1950 | 24370 | 49250 | 34.0 | 87.59 | 8.64 | 3.05 | 0.20 | 0.48 | 0.00 | |
| D3 | 1990 | 24120 | 49000 | 34.5 | 07.39 | 0.04 | 3.03 | 0.20 | 0.46 | 0.02 | (|
| E2 | 1900 | 24620 | 48500 | 29.0 | 97 50 | 8.64 | 3.05 | 0.20 | 0.48 | 0.00 | |
| | 1900 | | 43720 | | 87.59 | 0.04 | 3.03 | 0.20 | 0.48 | 0.02 | (|
| E3 | 1050 | 24620 | | 17.5 | 97 90 | 0 70 | 3.39 | 0.05 | 0.47 | 0.01 | (|
| F2 F3 | 1850 | 23620 24120 | 47990 | 27.0 27.0 | 87.30 | 8.76 | 3.39 | 0.05 | 0.47 | 0.01 | (|
| | from ano | ther heat beca | 47740 | | and on or | iginal hoat | | | | | |
| B2 | 2000 | | | | | | | | | | |
| | | 24370 25500 | 48740 49250 | 45.0 45.0 | 87.73 | 8.05 | 3.57 | 0.14 | 0.50 | 0.01 | 0 |
| B3 | 1950 | 25500 | 48740 49250 48740 | 45.0 45.0 32.0 | 87.73 87.73 | 8.05 8.05 | 3.57 3.57 | 0.14 | 0.50 | 0.01 | 0 |
| | 1950 | | 49250 | 45.0 | | | | | | | |
| B3 C2 | 1950 1900 | 25500 27270 | 49250 48740 | 45.0 32.0 | | | | | | | (|
| B3 C2 C3 | | 25500 27270 29150 | 49250 48740 47740 | 45.0 32.0 28.0 | 87.73 | 8.05 | 3.57 | 0.14 | 0.50 | 0.01 | (|
| B3 C2 C3 D2 | | 25500 27270 29150 29290 | 49250 48740 47740 48230 | 45.0 32.0 28.0 27.0 | 87.73 | 8.05 | 3.57 | 0.14 | 0.50 | 0.01 | (|
| B3 C2 C3 D2 D3 | 1900 | 25500 27270 29150 29290 30000 | 49250 48740 47740 48230 46250 | 45.0 32.0 28.0 27.0 21.0 | 87.73 87.60 | 8.05 8.27 | 3.57 3.47 | 0.14 0.15 | 0.50 | 0.01 | 0 |
| B3 C2 C3 D2 D3 E2 E3 | 1900 1850 | 25500 27270 29150 29290 30000 28640 29800 | 49250 48740 47740 48230 46250 47240 45460 at No. A86, Ladle | 45.0 32.0 28.0 27.0 21.0 23.0 20.5 Open-Flame | 87.73 87.60 87.60 87.60 | 8.05 8.27 8.27 ace, Tappin | 3.57 3.47 3.47 ng Temp. per 100 lb | 0.14 0.15 0.15 2500 F | 0.50 0.50 0.50 | 0.01 0.01 0.01 | 0 0 |
| B3 C2 C3 D2 D3 E2 E3 | 1900 | 25500 27270 29150 29290 30000 28640 29800 | 49250 48740 47740 48230 46250 47240 45460 at No. A86, Ladle 38750 | 45.0 32.0 28.0 27.0 21.0 23.0 20.5 Open-Flame Addition: Ph | 87.73 87.60 87.60 | 8.05 8.27 8.27 ace, Tappii | 3.57 3.47 3.47 Ing Temp. | 0.14 0.15 0.15 | 0.50 | 0.01 | 0 |
| B3 C2 C3 D2 D3 E2 E3 | 1900 1850 | 25500 27270 29150 29290 30000 28640 29800 | 49250 48740 47740 48230 46250 47240 45460 at No. A86, Ladle | 45.0 32.0 28.0 27.0 21.0 23.0 20.5 Open-Flame | 87.73 87.60 87.60 87.60 | 8.05 8.27 8.27 ace, Tappin | 3.57 3.47 3.47 ng Temp. per 100 lb | 0.14 0.15 0.15 2500 F | 0.50 0.50 0.50 | 0.01 0.01 0.01 | 0 |
| B3 C2 C3 D2 D3 E2 E3 | 1900 1850 2400 | 25500 27270 29150 29290 30000 28640 29800 He: | 49250 48740 47740 48230 46250 47240 45460 at No. A86, Ladle 38750 36500 | 45.0 32.0 28.0 27.0 21.0 23.0 20.5 Open-Flame Addition: Ph | 87.73 87.60 87.60 87.60 e, Oil-Furn cosphor-Cos | 8.05 8.27 8.27 nace, Tappin pper, 2 oz p 7.84 | 3.57 3.47 3.47 ng Temp. ner 100 lb 1.52 | 0.14 0.15 0.15 2500 F 0.09 | 0.50 0.50 0.50 | 0.01 0.01 0.01 | (|
| B3 C2 C3 D2 D3 E2 E3 A2 A3 B2 B3 | 1900 1850 2400 | 25500 27270 29150 29290 30000 28640 29800 He: 18500 18000 | 49250 48740 47740 48230 46250 47240 45460 at No. A86, Ladle 38750 36500 38500 | 45.0 32.0 28.0 27.0 21.0 23.0 20.5 Open-Flame Addition: Ph | 87.73 87.60 87.60 87.60 e, Oil-Furn cosphor-Cos | 8.05 8.27 8.27 nace, Tappin pper, 2 oz p 7.84 | 3.57 3.47 3.47 ng Temp. ner 100 lb 1.52 | 0.14 0.15 0.15 2500 F 0.09 | 0.50 0.50 0.50 | 0.01 0.01 0.01 0.01 | (|
| B3 C2 C3 D2 D3 E2 E3 | 1900 1850 2400 2350 | 25500 27270 29150 29290 30000 28640 29800 Hea 18500 18000 18500 18590 | 49250 48740 47740 48230 46250 47240 45460 at No. A86, Ladle 38750 36500 38500 39450 | 45.0 32.0 28.0 27.0 21.0 23.0 20.5 Open-Flame Addition: Ph 29.0 26.0 28.5 35.0 | 87.60 87.60 87.60 e, Oil-Furn osphor-Co 89.66 88.95 | 8.05 8.27 8.27 8.27 nace, Tappin pper, 2 oz p 7.84 7.88 | 3.57 3.47 3.47 og Temp. oer 100 lb 1.52 2.12 | 0.14 0.15 0.15 2500 F 0.09 0.17 | 0.50 0.50 0.50 0.88 0.87 | 0.01 0.01 0.01 | (|
| B3 C2 C3 D2 D3 E2 E3 A2 A3 B2 B3 C2 | 1900 1850 2400 2350 | 25500 27270 29150 29290 30000 28640 29800 He: 18500 18500 18500 18590 17750 | 49250 48740 47740 48230 46250 47240 45460 at No. A86, Ladle 38750 36500 38500 39450 34500 | 45.0 32.0 28.0 27.0 21.0 23.0 20.5 Open-Flame Addition: Ph 29.0 26.0 28.5 35.0 25.5 | 87.60 87.60 87.60 e, Oil-Furn osphor-Co 89.66 88.95 | 8.05 8.27 8.27 8.27 nace, Tappin pper, 2 oz p 7.84 7.88 | 3.57 3.47 3.47 og Temp. oer 100 lb 1.52 2.12 | 0.14 0.15 0.15 2500 F 0.09 0.17 | 0.50 0.50 0.50 0.88 0.87 | 0.01 0.01 0.01 0.01 | |
| B3 C2 C3 D2 D3 E2 E3 A2 A3 B2 B3 C2 C3 | 1900 1850 2400 2350 2300 | 25500 27270 29150 29290 30000 28640 29800 He: 18500 18500 18500 18590 17750 18340 | 49250 48740 47740 48230 46250 47240 45460 at No. A86, Ladle 38750 36500 38500 39450 34500 37690 | 45.0 32.0 28.0 27.0 21.0 23.0 20.5 Open-Flame Addition: Ph 29.0 26.0 28.5 35.0 25.5 29.0 | 87.73 87.60 87.60 87.60 87.60 89.66 88.95 88.37 | 8.05 8.27 8.27 8.27 nace, Tappin pper, 2 oz p 7.84 7.88 8.06 | 3.57 3.47 3.47 Temp. er 100 lb 1.52 2.12 2.56 | 0.14 0.15 0.15 2500 F 0.09 0.17 0.14 | 0.50 0.50 0.50 0.88 0.87 0.86 | 0.01 0.01 0.01 0.01 0.01 | 0 0 0 0 |
| B3 C2 C3 D2 D3 E2 E3 A2 A3 B2 B3 C2 C3 D2 | 1900 1850 2400 2350 2300 | 25500 27270 29150 29290 30000 28640 29800 He: 18500 18500 18590 17750 18340 19000 | 49250 48740 47740 48230 46250 47240 45460 at No. A86, Ladle 38750 36500 38500 39450 34500 37690 42250 | 45.0 32.0 28.0 27.0 21.0 23.0 20.5 Open-Flame Addition: Ph 29.0 26.0 28.5 35.0 25.5 29.0 37.5 | 87.73 87.60 87.60 87.60 87.60 89.66 88.95 88.37 | 8.05 8.27 8.27 8.27 nace, Tappin pper, 2 oz p 7.84 7.88 8.06 | 3.57 3.47 3.47 Temp. er 100 lb 1.52 2.12 2.56 | 0.14 0.15 0.15 2500 F 0.09 0.17 0.14 | 0.50 0.50 0.50 0.88 0.87 0.86 | 0.01 0.01 0.01 0.01 0.01 | 0 0 0 0 0 0 |
| B3 C2 C3 D2 D3 E2 E3 A2 A3 B2 B3 C2 C3 D2 D3 E2 | 1900 1850 2400 2350 2300 2250 | 25500 27270 29150 29290 30000 28640 29800 He: 18500 18500 18590 17750 18340 19000 19600 20000 19350 | 49250 48740 47740 48230 46250 47240 45460 at No. A86, Ladle 38750 36500 38500 39450 34500 37690 42250 40600 | 45.0 32.0 28.0 27.0 21.0 23.0 20.5 Open-Flame Addition: Ph 29.0 26.0 28.5 35.0 25.5 29.0 37.5 32.5 49.0 42.0 | 87.73 87.60 87.60 87.60 87.60 89.66 88.95 88.37 88.95 | 8.05 8.27 8.27 8.27 nace, Tappin pper, 2 oz p 7.84 7.88 8.06 7.88 | 3.57 3.47 3.47 3.47 Ing Temp. Inser 100 lb 1.52 2.12 2.56 2.12 2.56 | 0.14 0.15 0.15 2500 F 0.09 0.17 0.14 0.17 | 0.50 0.50 0.50 0.88 0.87 0.86 0.87 | 0.01 0.01 0.01 0.01 0.01 0.01 0.01 | 0 0 0 0 0 0 |
| B3 C2 C3 D2 D3 E2 E3 A2 A3 B2 B3 C2 C3 D2 D3 E2 E3 E2 E3 E2 E3 E2 E3 E2 E3 E2 E3 | 1900 1850 2400 2350 2300 2250 | 25500 27270 29150 29290 30000 28640 29800 Hes 18500 18000 18590 17750 18340 19000 19600 20000 | 49250 48740 47740 48230 46250 47240 45460 at No. A86, Ladle 38750 36500 38500 39450 34500 37690 42250 40600 45500 | 45.0 32.0 28.0 27.0 21.0 23.0 20.5 Open-Flame Addition: Ph 29.0 26.0 28.5 35.0 25.5 29.0 37.5 32.5 49.0 | 87.73 87.60 87.60 87.60 87.60 89.66 88.95 88.37 88.95 | 8.05 8.27 8.27 8.27 Pace, Tappin pper, 2 oz p 7.84 7.88 8.06 7.88 | 3.57 3.47 3.47 3.47 ng Temp. per 100 lb 1.52 2.12 2.56 2.12 | 0.14 0.15 0.15 2500 F 0.09 0.17 0.14 0.17 | 0.50 0.50 0.50 0.88 0.87 0.86 0.87 | 0.01 0.01 0.01 0.01 0.01 0.01 | |
| B3 C2 C3 D2 D3 E2 E3 A2 A3 B2 B3 C2 C3 D2 D3 E2 E3 F2 F3 | 1900 1850 2400 2350 2300 2250 2200 2150 | 25500 27270 29150 29290 30000 28640 29800 He: 18500 18500 18590 17750 18340 19000 19600 20000 19350 19000 | 49250 48740 47740 48230 46250 47240 45460 at No. A86, Ladle 38750 36500 38500 39450 34500 37690 42250 40600 45500 44720 | 45.0 32.0 28.0 27.0 21.0 23.0 20.5 Open-Flame Addition: Ph 29.0 26.0 28.5 35.0 25.5 29.0 37.5 32.5 49.0 42.0 45.5 44.0 | 87.73 87.60 87.60 87.60 87.60 89.66 88.95 88.37 88.95 88.37 | 8.05 8.27 8.27 8.27 Pace, Tappin pper, 2 oz p 7.84 7.88 8.06 7.88 8.06 7.88 | 3.57 3.47 3.47 3.47 Ing Temp. Per 100 lb 1.52 2.12 2.56 2.12 2.56 2.12 | 0.14 0.15 0.15 2500 F 0.09 0.17 0.14 0.17 | 0.50 0.50 0.50 0.88 0.87 0.86 0.87 | 0.01 0.01 0.01 0.01 0.01 0.01 0.01 | |
| B3 C2 C3 D2 D3 E2 E3 A2 A3 B2 B3 C2 C3 D2 D3 E2 E3 F2 F3 G2 | 1900 1850 2400 2350 2300 2250 2200 | 25500 27270 29150 29290 30000 28640 29800 Healer State | 49250 48740 47740 48230 46250 47240 45460 at No. A86, Ladle 38750 36500 38500 39450 34500 34500 47290 46250 46500 46500 | 45.0 32.0 28.0 27.0 21.0 23.0 20.5 Open-Flame Addition: Ph 29.0 26.0 28.5 35.0 25.5 29.0 37.5 32.5 49.0 42.0 45.5 44.0 | 87.73 87.60 87.60 87.60 2, Oil-Furn cosphor-Cos 89.66 88.95 88.37 88.95 | 8.05 8.27 8.27 8.27 nace, Tappin pper, 2 oz p 7.84 7.88 8.06 7.88 | 3.57 3.47 3.47 3.47 Ing Temp. Inser 100 lb 1.52 2.12 2.56 2.12 2.56 | 0.14 0.15 0.15 2500 F 0.09 0.17 0.14 0.17 | 0.50 0.50 0.50 0.88 0.87 0.86 0.87 | 0.01 0.01 0.01 0.01 0.01 0.01 0.01 | 000000000000000000000000000000000000000 |
| B3 C2 C3 D2 D3 E2 E3 A2 A3 B2 B3 C2 C3 D2 D3 E2 E3 F2 F3 G2 G3 | 1900 1850 2400 2350 2300 2250 2200 2150 2100 | 25500 27270 29150 29290 30000 28640 29800 Hes 18500 18500 18590 17750 18340 19000 19600 20000 19350 19000 19500 18500 18500 | 49250 48740 47740 48230 46250 47240 45460 at No. A86, Ladle 38750 36500 38500 39450 34500 37690 42250 40600 45500 44720 46500 44500 45500 | 45.0 32.0 28.0 27.0 21.0 23.0 20.5 Open-Flame Addition: Ph 29.0 26.0 28.5 35.0 25.5 29.0 37.5 32.5 49.0 42.0 45.5 44.0 46.5 40.0 | 87.73 87.60 87.60 87.60 87.60 89.66 88.95 88.37 88.95 88.37 88.95 88.37 | 8.05 8.27 8.27 8.27 Pace, Tappin pper, 2 oz p 7.84 7.88 8.06 7.88 8.06 7.88 8.06 | 3.57 3.47 3.47 3.47 ng Temp. per 100 lb 1.52 2.12 2.56 2.12 2.56 2.12 2.56 | 0.14 0.15 0.15 2500 F 0.09 0.17 0.14 0.17 0.14 0.17 | 0.50 0.50 0.50 0.88 0.87 0.86 0.87 0.86 | 0.01 0.01 0.01 0.01 0.01 0.01 0.01 0.01 | 000000000000000000000000000000000000000 |
| B3 C2 C3 D2 D3 E2 E3 A2 A3 B2 B3 C2 C3 D2 D3 E2 F3 F2 F3 G2 G3 H2 | 1900 1850 2400 2350 2300 2250 2200 2150 | 25500 27270 29150 29290 30000 28640 29800 He: 18500 18500 18500 18590 17750 18340 19000 19600 20000 19350 19000 19500 18500 18500 | 49250 48740 47740 48230 46250 47240 45460 at No. A86, Ladle 38750 36500 38500 38500 39450 34500 42250 40600 44720 46500 44500 44500 48000 | 45.0 32.0 28.0 27.0 21.0 23.0 20.5 Open-Flame Addition: Ph 29.0 26.0 28.5 35.0 25.5 29.0 37.5 32.5 49.0 42.0 45.5 44.0 46.5 40.0 52.0 | 87.73 87.60 87.60 87.60 87.60 89.66 88.95 88.37 88.95 88.37 | 8.05 8.27 8.27 8.27 Pace, Tappin pper, 2 oz p 7.84 7.88 8.06 7.88 8.06 7.88 | 3.57 3.47 3.47 3.47 Ing Temp. Per 100 lb 1.52 2.12 2.56 2.12 2.56 2.12 | 0.14 0.15 0.15 2500 F 0.09 0.17 0.14 0.17 | 0.50 0.50 0.50 0.88 0.87 0.86 0.87 | 0.01 0.01 0.01 0.01 0.01 0.01 0.01 | 000000000000000000000000000000000000000 |
| B3 C2 C3 D2 D3 E2 E3 A2 A3 B2 B3 CC C3 D2 D3 E2 E3 F2 F3 G2 G3 H2 H3 | 1900 1850 2400 2350 2300 2250 2200 2150 2100 2050 | 25500 27270 29150 29290 30000 28640 29800 Hes 18500 18500 18590 17750 18340 19000 19600 20000 19350 19000 19500 18590 18750 18750 20500 | 49250 48740 47740 48230 46250 47240 45460 at No. A86, Ladle 38750 36500 38500 39450 34500 34500 42250 40600 45500 44500 45500 45500 48000 50500 | 45.0 32.0 28.0 27.0 21.0 23.0 20.5 Open-Flame Addition: Ph 29.0 26.0 28.5 35.0 25.5 29.0 37.5 32.5 49.0 45.5 44.0 46.5 40.0 52.0 48.5 | 87.73 87.60 87.60 87.60 89.66 88.95 88.37 88.95 88.37 88.95 | 8.05 8.27 8.27 8.27 9 20 2 p 7.84 7.88 8.06 7.88 8.06 7.88 8.06 7.88 | 3.57 3.47 3.47 3.47 Ing Temp. ser 100 lb 1.52 2.12 2.56 2.12 2.56 2.12 2.56 2.12 | 0.14 0.15 0.15 2500 F 0.09 0.17 0.14 0.17 0.14 0.17 | 0.50 0.50 0.50 0.88 0.87 0.86 0.87 0.86 0.87 | 0.01 0.01 0.01 0.01 0.01 0.01 0.01 0.01 | |
| B3 C2 C3 D2 D3 E2 E3 A2 A3 B2 B3 C2 C3 D2 D3 E2 E3 F2 F3 G2 G3 H2 H3 H2 | 1900 1850 2400 2350 2300 2250 2200 2150 2100 | 25500 27270 29150 29290 30000 28640 29800 He: 18500 18500 18590 17750 18340 19000 19600 20000 19500 18500 18750 20500 20500 | 49250 48740 47740 48230 46250 47240 45460 at No. A86, Ladle 38750 36500 38500 39450 34500 37690 42250 40600 45500 44720 46500 44500 455 | 45.0 32.0 28.0 27.0 21.0 23.0 20.5 Open-Flame Addition: Ph 29.0 26.0 28.5 35.0 25.5 29.0 37.5 32.5 49.0 45.5 44.0 46.5 40.0 52.0 48.5 53.5 | 87.73 87.60 87.60 87.60 87.60 89.66 88.95 88.37 88.95 88.37 88.95 88.37 | 8.05 8.27 8.27 8.27 Pace, Tappin pper, 2 oz p 7.84 7.88 8.06 7.88 8.06 7.88 8.06 | 3.57 3.47 3.47 3.47 ng Temp. per 100 lb 1.52 2.12 2.56 2.12 2.56 2.12 2.56 | 0.14 0.15 0.15 2500 F 0.09 0.17 0.14 0.17 0.14 0.17 | 0.50 0.50 0.50 0.88 0.87 0.86 0.87 0.86 | 0.01 0.01 0.01 0.01 0.01 0.01 0.01 0.01 | |
| B3 C2 C3 D2 D3 E2 E3 A2 A3 B2 B3 C2 C3 D2 D3 E2 E3 F2 F3 G2 G3 H2 H3 H2 H3 H2 H3 H2 H3 H2 H3 H2 H3 H2 H3 H2 H3 H2 H3 H3 H3 H4 H3 H4 H3 H4 H3 H4 H3 H4 H3 H4 H3 H4 H4 H4 H4 H4 H4 H4 H4 H4 H4 H4 H4 H4 | 2400 2350 2300 2250 2200 2150 2100 2050 | 25500 27270 29150 29290 30000 28640 29800 Hes 18500 18500 18500 18590 17750 18340 19000 19600 20000 19350 19000 19500 18500 18750 20500 20250 20410 | 49250 48740 47740 48230 46250 47240 45460 at No. A86, Ladle 38750 36500 38500 39450 34500 34500 4720 46500 44720 46500 4550 | 45.0 32.0 28.0 27.0 21.0 23.0 20.5 Open-Flame Addition: Ph 29.0 26.0 28.5 35.0 25.5 29.0 37.5 32.5 49.0 42.0 45.5 44.0 46.5 40.0 52.0 48.5 53.5 | 87.73 87.60 87.60 87.60 87.60 87.60 89.66 88.95 88.37 88.95 88.37 88.95 88.37 88.95 | 8.05 8.27 8.27 8.27 900 pper, 2 oz p 7.84 7.88 8.06 7.88 8.06 7.88 8.06 7.88 | 3.57 3.47 3.47 3.47 ng Temp. per 100 lb 1.52 2.12 2.56 2.12 2.56 2.12 2.56 2.12 | 0.14 0.15 0.15 2500 F 0.09 0.17 0.14 0.17 0.14 0.17 0.14 | 0.50 0.50 0.50 0.88 0.87 0.86 0.87 0.86 0.87 0.86 | 0.01 0.01 0.01 0.01 0.01 0.01 0.01 0.01 | |
| B3 C2 C3 D2 D3 E2 E3 A2 A3 B2 B3 C2 C3 D2 D3 E2 E3 F2 F3 G2 G3 H2 H3 I2 H3 I2 | 1900 1850 2400 2350 2300 2250 2200 2150 2100 2050 | 25500 27270 29150 29290 30000 28640 29800 Hes 18500 18500 18590 17750 18340 19000 19600 20000 19350 19000 19500 18500 18750 18750 20500 20250 20410 20750 | 49250 48740 47740 48230 46250 47240 45460 at No. A86, Ladle 38750 36500 38500 39450 34500 37690 42250 40600 4720 46500 44500 44500 45500 5050 | 45.0 32.0 28.0 27.0 21.0 23.0 20.5 Open-Flame Addition: Ph 29.0 26.0 28.5 35.0 25.5 29.0 37.5 32.5 49.0 42.0 45.5 44.0 46.5 40.0 52.0 48.5 53.5 | 87.73 87.60 87.60 87.60 89.66 88.95 88.37 88.95 88.37 88.95 | 8.05 8.27 8.27 8.27 9 20 2 p 7.84 7.88 8.06 7.88 8.06 7.88 8.06 7.88 | 3.57 3.47 3.47 3.47 Ing Temp. ser 100 lb 1.52 2.12 2.56 2.12 2.56 2.12 2.56 2.12 | 0.14 0.15 0.15 2500 F 0.09 0.17 0.14 0.17 0.14 0.17 | 0.50 0.50 0.50 0.88 0.87 0.86 0.87 0.86 0.87 | 0.01 0.01 0.01 0.01 0.01 0.01 0.01 0.01 | |
| B3 C2 C3 D2 D3 E2 E3 A2 A3 B2 B3 C2 C3 D2 D3 E2 E3 F2 F3 G2 G3 H2 H3 I2 I3 I2 J3 | 1900 1850 2400 2350 2300 2250 2200 2150 2100 2050 2000 | 25500 27270 29150 29290 30000 28640 29800 Hea 18500 18500 18500 18590 17750 18340 19000 19600 20000 19350 19000 19500 18750 20500 20250 20410 20750 20100 | 49250 48740 47740 48230 46250 47240 45460 at No. A86, Ladle 38750 36500 38500 39450 34500 34500 42250 40600 45500 44720 46500 44500 44500 50500 51500 50500 51250 50750 | 45.0 32.0 28.0 27.0 21.0 23.0 20.5 Open-Flame Addition: Ph 29.0 26.0 28.5 35.0 25.5 29.0 37.5 32.5 49.0 46.5 44.0 46.5 40.0 52.0 48.5 53.5 49.0 40.0 | 87.73 87.60 87.60 87.60 87.60 89.66 88.95 88.37 88.95 88.37 88.95 88.37 88.95 88.37 | 8.05 8.27 8.27 8.27 9.20 p 7.84 7.88 8.06 7.88 8.06 7.88 8.06 7.88 | 3.57 3.47 3.47 3.47 Temp. 100 lb 1.52 2.12 2.56 2.12 2.56 2.12 2.56 2.12 2.56 1.52 | 0.14 0.15 0.15 2500 F 0.09 0.17 0.14 0.17 0.14 0.17 0.14 0.17 | 0.50 0.50 0.50 0.88 0.87 0.86 0.87 0.86 0.87 0.86 0.87 | 0.01 0.01 0.01 0.01 0.01 0.01 0.01 0.01 0.01 0.01 | 0 0 0 0 0 0 0 0 0 |
| B3 C2 C3 D2 D3 E2 E3 A2 A3 B2 B3 C2 C3 D2 D3 E2 E3 F2 F3 G2 G3 H2 H3 H2 H3 H2 H3 H2 H3 H2 H3 H2 H3 H2 H3 H2 H3 H2 H3 H2 H3 H4 H3 H4 H3 H4 H3 H4 H3 H4 H3 H4 H4 H4 H4 H4 H4 H4 H4 H4 H4 H4 H4 H4 | 2400 2350 2300 2250 2200 2150 2100 2050 | 25500 27270 29150 29290 30000 28640 29800 He: 18500 18500 18590 17750 18340 19000 19600 20000 19350 19000 19500 18750 18750 20500 20250 20410 20750 20100 18250 | 49250 48740 47740 48230 46250 47240 45460 at No. A86, Ladle 38750 36500 38500 39450 34500 37690 42250 40600 45500 44720 46500 44500 45500 44500 50500 50500 51500 50500 51250 50750 47750 | 45.0 32.0 28.0 27.0 21.0 23.0 20.5 Open-Flame Addition: Ph 29.0 26.0 28.5 35.0 25.5 29.0 37.5 32.5 49.0 46.5 40.0 46.5 40.0 52.0 48.5 53.5 49.0 40.0 52.0 40.0 53.5 53.5 53.5 53.5 53.5 53.5 53.5 53.5 53.5 53.0 | 87.73 87.60 87.60 87.60 87.60 87.60 89.66 88.95 88.37 88.95 88.37 88.95 88.37 88.95 | 8.05 8.27 8.27 8.27 900 pper, 2 oz p 7.84 7.88 8.06 7.88 8.06 7.88 8.06 7.88 | 3.57 3.47 3.47 3.47 ng Temp. per 100 lb 1.52 2.12 2.56 2.12 2.56 2.12 2.56 2.12 | 0.14 0.15 0.15 2500 F 0.09 0.17 0.14 0.17 0.14 0.17 0.14 | 0.50 0.50 0.50 0.88 0.87 0.86 0.87 0.86 0.87 0.86 | 0.01 0.01 0.01 0.01 0.01 0.01 0.01 0.01 | |
| B3 C2 C3 D2 D3 E2 E3 A2 A3 B2 B3 C2 C3 D2 D3 E2 E3 E3 E2 E3 E3 E2 E3 E3 E2 E3 E3 E2 E3 E3 E4 E4 E5 E5 E5 E5 E5 E5 E5 E5 E5 E5 E5 E5 E5 | 1900 1850 2400 2350 2350 2250 2250 2150 2100 2050 2000 1950 | 25500 27270 29150 29290 30000 28640 29800 Hes 18500 18500 18500 18590 17750 18340 19000 19600 20000 19500 18500 18750 18750 20500 20250 20410 20750 20100 18250 19500 | 49250 48740 47740 48230 46250 47240 45460 at No. A86, Ladle 38750 36500 38500 39450 34500 37690 42250 40600 45500 44720 46500 44500 45500 45500 50500 51500 50000 51250 50750 47750 50500 | 45.0 32.0 28.0 27.0 21.0 23.0 20.5 Open-Flame Addition: Ph 29.0 28.5 35.0 25.5 29.0 37.5 32.5 49.0 45.5 44.0 46.5 40.0 52.0 48.5 53.5 | 87.73 87.60 87.60 87.60 87.60 89.66 88.95 88.37 88.95 88.37 88.95 88.37 88.95 88.37 88.95 | 8.05 8.27 8.27 8.27 900 per, 2 oz pe | 3.57 3.47 3.47 3.47 3.47 ng Temp. er 100 lb 1.52 2.12 2.56 2.12 2.56 2.12 2.56 2.12 1.52 | 0.14 0.15 0.15 2500 F 0.09 0.17 0.14 0.17 0.14 0.17 0.14 0.17 0.14 0.09 | 0.50 0.50 0.50 0.88 0.87 0.86 0.87 0.86 0.87 0.86 0.87 0.86 0.87 | 0.01 0.01 0.01 0.01 0.01 0.01 0.01 0.01 0.01 0.01 0.01 | 000000000000000000000000000000000000000 |
| B3 C2 C3 D2 D3 E2 E3 A2 A3 B2 B3 C2 C3 D2 D3 E2 E3 F2 F3 G2 G3 H2 H3 I2 I3 J2 J3 K2 K3 | 1900 1850 2400 2350 2300 2250 2200 2150 2100 2050 2000 | 25500 27270 29150 29290 30000 28640 29800 He: 18500 18500 18590 17750 18340 19000 19600 20000 19350 19000 19500 18750 18750 20500 20250 20410 20750 20100 18250 | 49250 48740 47740 48230 46250 47240 45460 at No. A86, Ladle 38750 36500 38500 39450 34500 37690 42250 40600 45500 44720 46500 44500 45500 44500 50500 50500 51500 50500 51250 50750 47750 | 45.0 32.0 28.0 27.0 21.0 23.0 20.5 Open-Flame Addition: Ph 29.0 26.0 28.5 35.0 25.5 29.0 37.5 32.5 49.0 46.5 40.0 46.5 40.0 52.0 48.5 53.5 49.0 40.0 52.0 40.0 53.5 53.5 53.5 53.5 53.5 53.5 53.5 53.5 53.5 53.0 | 87.73 87.60 87.60 87.60 87.60 89.66 88.95 88.37 88.95 88.37 88.95 88.37 88.95 88.37 | 8.05 8.27 8.27 8.27 9.20 p 7.84 7.88 8.06 7.88 8.06 7.88 8.06 7.88 | 3.57 3.47 3.47 3.47 Temp. 100 lb 1.52 2.12 2.56 2.12 2.56 2.12 2.56 2.12 2.56 1.52 | 0.14 0.15 0.15 2500 F 0.09 0.17 0.14 0.17 0.14 0.17 0.14 0.17 | 0.50 0.50 0.50 0.88 0.87 0.86 0.87 0.86 0.87 0.86 0.87 | 0.01 0.01 0.01 0.01 0.01 0.01 0.01 0.01 0.01 0.01 | |

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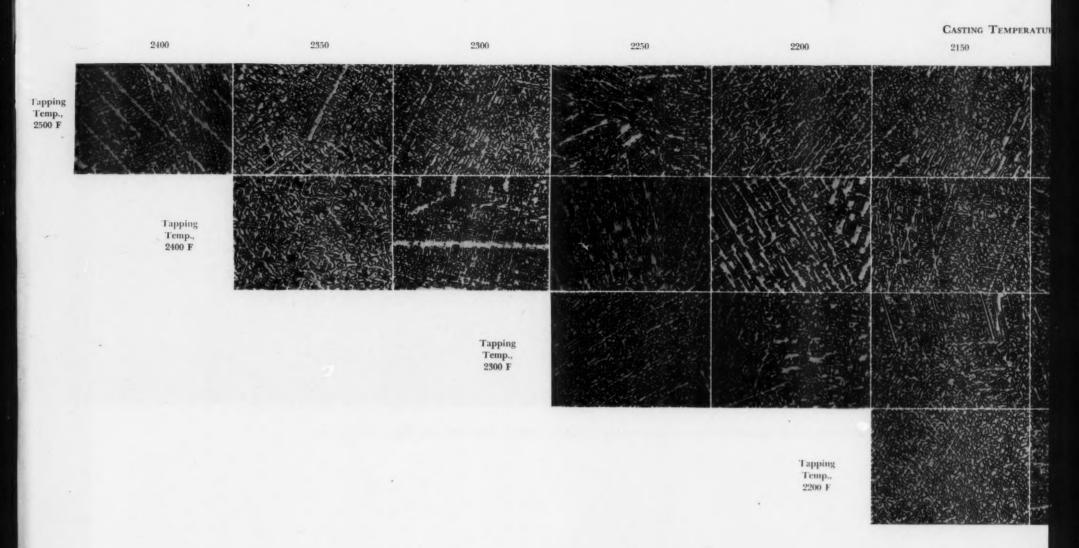
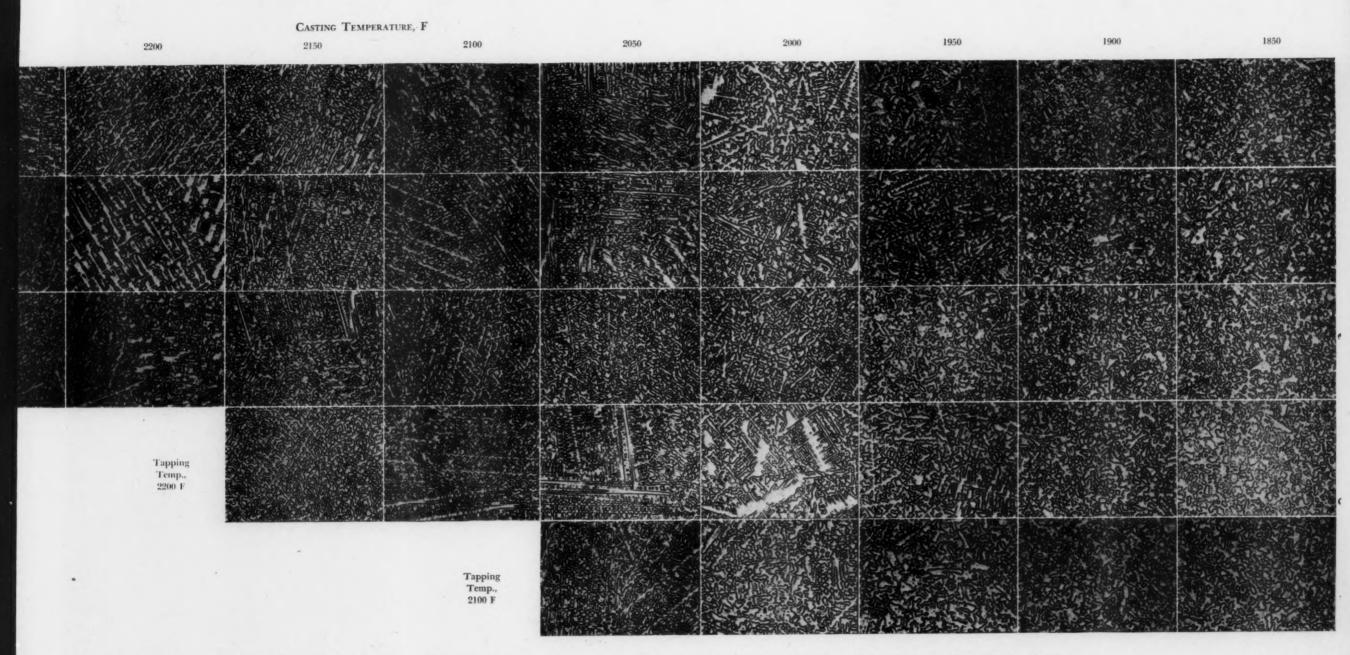


Fig. 10-Effect of Tapping and Casting Temperatures on the Microstructure of Transverse Sections of Gun Metal Test Bars. 2 oz/100



RUCTURE OF TRANSVERSE SECTIONS OF GUN METAL TEST BARS. 2 OZ/100 LB PHOSPHOR-COPPER ADDED TO LADLE. MAGNIFICATION: X 25. ETCHANT: NH4OH + H2O2. FURNACE: OIL-FIRED, OPEN-FLAME.

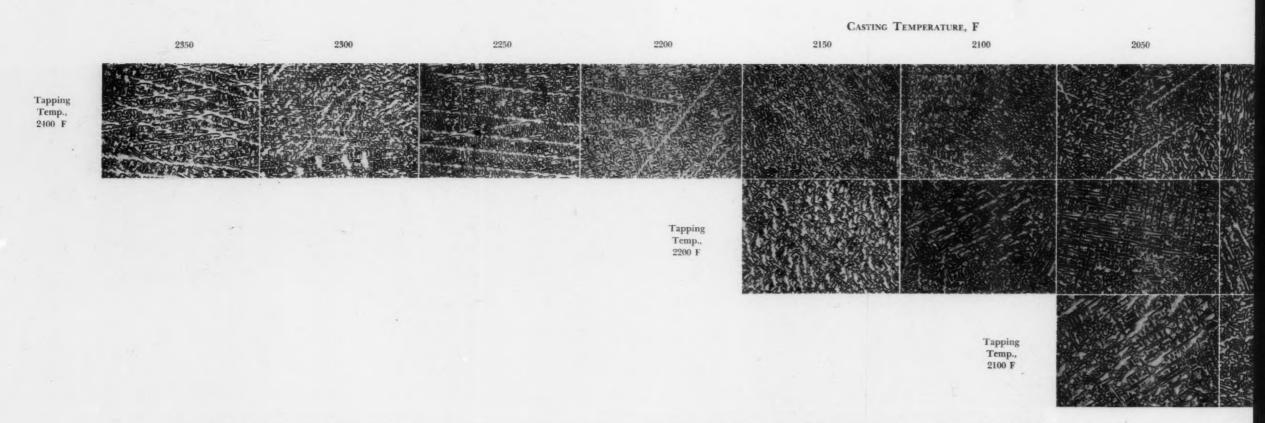
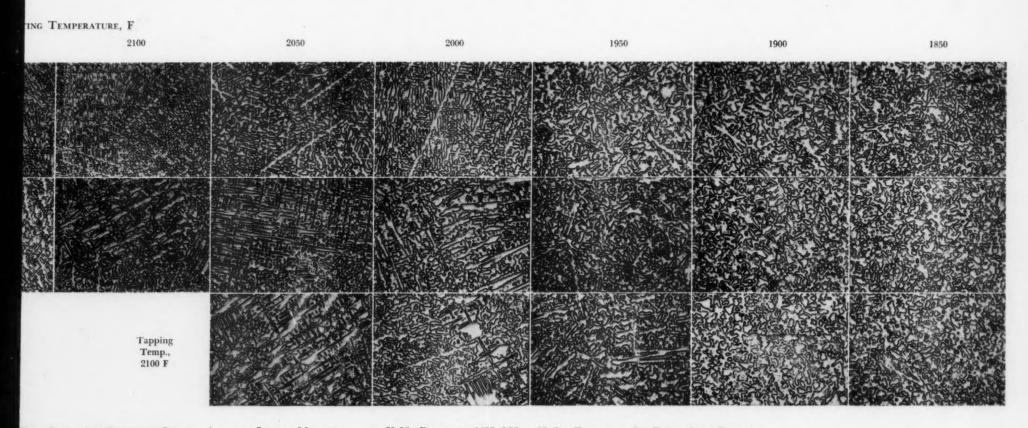


Fig. 11-Effect of Tapping and Casting Temperatures on the Microstructure of Transverse Sections of Gun Metal Test Bars. No Phosphor-Copper Added to Ladle, Magnification: X 2.



Test Bars. No Phosphor-Copper Added to Ladle. Magnification: X 25. Etchant: NH4OH + H2O2. Furnace: Oil-Fired, Open-Flame.

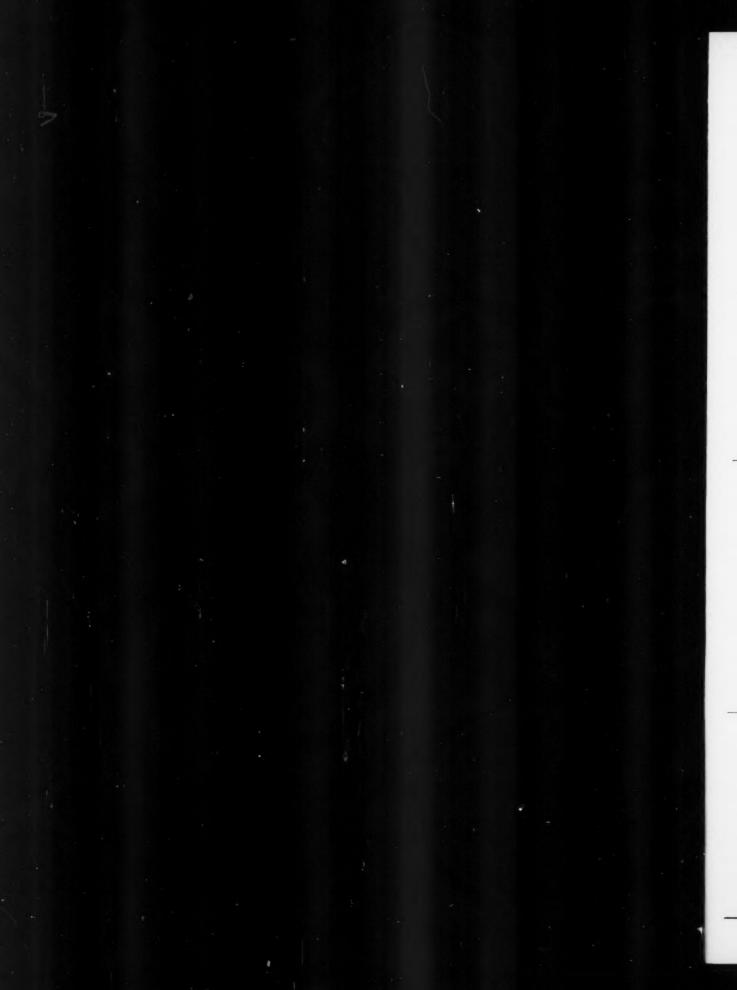


Table 1 (cont.)—Physical and Chemical Properties of Gun Metal as Affected by Melting Unit, Tapping and Casting Temperatures and Deoxidation

| Test Bar No. | | | ysical Proper Tensile | ties | | | Chen | nical Analy | ses, Per Ce | nt | |
|---|--------------------------------------|---|---|---|---|---|--|--|--|--|--|
| | Casting Temp. °F | Yield Poin psi | t, Strength, psi | Elongation, % in 2 in. | Cu | Sn | Zn | Pb | Ni | Fe | |
| | | | Heat No. A | 82, Open-Fla | me, Oil-Fi | urnace, Taj | oping Tem | p. 2400 F | | | |
| | | | Lad | e Addition: | Phosphor- | Copper, 2 o | z per 100 1 | b | | | |
| A2 | 2350 | 17500 | 24000 | 15.0 | 89.37 | 7.83 | 2.17 | 0.19 | 0.49 | 0.01 | |
| A3 | | 17590 | 32660 | 22.0 | | * 100 | Ma. A. F | 0.19 | 0.43 | 0.01 | 0 |
| B2 B3 | 2300 | 19100 | 37160 | 30.0 | 89.37 | 7.83 | 2.17 | 0.19 | 0.43 | 0.01 | 0 |
| C2 | 2250 | 19100 | 34670 | 25.5 | | | | | 0110 | 0.01 | |
| C3 | 4430 | 19100 19100 | 43470 42210 | 47.0 | 89.27 | 7.73 | 2.38 | 0.17 | 0.43 | 0.01 | 0 |
| D2 | 2200 | 19190 | 41920 | 43.5 43.0 | 89.27 | m ma | 0.00 | | | | |
| D3 | | 19000 | 42750 | 44.0 | 09.27 | 7.73 | 2.38 | 0.17 | 0.43 | 0.01 | 0 |
| E2 | 2150 | 19100 | 42460 | 37.5 | 89.27 | 7.73 | 2.38 | 0.17 | 0.43 | 0.01 | |
| E3 | | 19000 | 42250 | 41.5 | | **** | 2.00 | 0.17 | 0.43 | 0.01 | 0 |
| F2 | 2100 | 20100 | 44720 | 46.0 | 89.37 | 7.83 | 2.17 | 0.19 | 0.43 | 0.01 | 0 |
| F3 G2 | 2050 | 20600 | 42710 | 34.5 | | | | | | 0.02 | 0 |
| G3 | 2030 | 20500 20000 | 47500 | 58.0 | 89.85 | 7.72 | 2.19 | 0.19 | 0.43 | 0.01 | 0 |
| H2 | 2000 | 21110 | 44000 46730 | 39.5 | 00.04 | | - | | | | |
| H3 | 2000 | 21110 | 48740 | 48.5 | 89.85 | 7.72 | 2.19 | 0.19 | 0.43 | 0.01 | 0 |
| 12 | 1950 | 22220 | 44190 | 52.5 26.0 | 89.85 | m ma | 0.10 | | | | |
| 13 | | 21610 | 50000 | 46.5 | 09.00 | 7.72 | 2.19 | 0.19 | 0.43 | 0.01 | 0 |
| J2 | 1900 | 22000 | 50000 | 42.0 | 90.43 | 7.55 | 1.36 | 0.21 | 0.49 | 0.01 | |
| J3 | | 22610 | 47990 | 33.0 | 00110 | 7.33 | 1.30 | 0.21 | 0.43 | 0.01 | 0. |
| K2 | 1850 | 22500 | 45500 | 27.5 | 90.43 | 7.55 | 1.36 | 0.21 | 0.43 | 0.01 | 0. |
| К3 | | 22340 | 45940 | 29.5 | | | | | | | |
| A2 A3 | 2250 | 21500 21610 | 45500 42710 | 50.5 36.0 | 88.52 | 8.22 | 2.53 | 0.18 | 0.52 | 0.02 | 0. |
| B2 B3 | 2200 | 21110 | 43970 | 42.0 | 88.15 | 8.22 | 30.1 | 0.09 | 0.50 | 0.02 | 0.0 |
| C2 | 2150 | 21610 22610 | 44720 | 43.0 | | | | | | 0104 | 0.1 |
| C3 | 2150 | 21500 | 45230 45750 | 42.0 | 88.15 | 8.22 | 30.1 | 0.09 | 0.50 | 0.02 | 0.0 |
| D2 | 2100 | 22500 | 46250 | 46.5 44.5 | 88.15 | 0.00 | 90.1 | 0.00 | | | |
| D3 | | 22110 | 46480 | 46.0 | 00.13 | 8.22 | 30.1 | 0.09 | 0.50 | 0.02 | 0.0 |
| E2 | 2050 | 23120 | 49000 | 54.0 | 88.52 | 8.22 | 2.53 | 0.18 | 0.52 | 0.00 | |
| E3 | 2000 | 22750 | 48250 | 53.0 | | | 4.00 | 0.16 | 0.34 | 0.02 | 0.0 |
| F2 F3 | 2000 | 24000 | 50750 | 51.0 | 88.52 | 8.22 | 2.53 | 0.18 | 0.52 | 0.02 | |
| ro | 1950 | 24000 | 46500 | 34.0 | | | | | | 0.00 | 0.0 |
| C2 | 1930 | 24120 24120 | 49250 | 37.0 | 88.70 | | | 0.07 | 0.50 | | 0.0 |
| | 1000 | 24120 | 40500 | | 00.70 | 8.38 | 2.32 | 0.07 | 0.00 | 0.02 | |
| G3- | 1900 | | 49500 | 37.5 | | | | | | 0.02 | |
| G3- H2 | 1900 | 24120 | 46230 | 37.5 23.5 | 88.70 | 8.38 8.38 | 2.32 | 0.07 | 0.50 | 0.02 | 0.0 |
| G3• H2 H3 | 1850 | | 46230 45500 | 37.5 23.5 22.0 | 88.70 | 8.38 | 2.32 | 0.07 | 0.50 | 0.02 | 0.0 |
| G3- H2 H3 | | 24120 22000 | 46230 | 37.5 23.5 | | | | | | | 0.0 |
| G3• H2 H3 I2 | | 24120 22000 24260 23620 | 46230 45500 49000 46730 at No. A81, | 37.5 23.5 22.0 31.0 25.5 | 88.70 88.70 | 8.38 8.38 | 2.32 2.32 | 0.07 | 0.50 | 0.02 | 0.0 |
| | | 24120 22000 24260 23620 He | 46230 45500 49000 46730 at No. A81, Ladle A | 37.5 23.5 22.0 31.0 25.5 Open-Flame ddition: Ph | 88.70 88.70 | 8.38 8.38 | 2.32 2.32 | 0.07 | 0.50 | 0.02 | 0.0 |
| G3- H2 H3 I2 I3 | 1850 2150 | 24120 22000 24260 23620 He 21110 21110 | 46230 45500 49000 46730 at No. A81, Ladle A 45730 45730 | 37.5 23.5 22.0 31.0 25.5 Open-Flame ddition: Ph 42.0 44.0 | 88.70 88.70 e, Oil-Furn osphor-Cop 88.23 | 8.38 8.38 ace, Tappin oper, 2 oz p 7.95 | 2.32 2.32 2.32 ng Temp. : | 0.07 0.07 2200 F 0.06 | 0.50 | 0.02 | 0.0 |
| G3- H2 H3 12 13 | 1850 | 24120 22000 24260 23620 He 21110 21110 20200 | 46230 45500 49000 46730 at No. A81, Ladle A 45730 45730 45710 | 37.5 23.5 22.0 31.0 25.5 Open-Flame ddition: Ph 42.0 44.0 48.0 | 88.70 88.70 c, Oil-Furn osphor-Cop | 8.38 8.38 ace, Tappin oper, 2 oz p | 2.32 2.32 ng Temp. 2 | 0.07 0.07 2200 F | 0.50 | 0.02 | 0.0 0.0 0.0 0.0 0.0 |
| G3+ H2 H3 12 23 3 | 1850 2150 | 24120 22000 24260 23620 He 21110 21110 | 46230 45500 49000 46730 at No. A81, Ladle A 45730 45710 47220 | 37.5 23.5 22.0 31.0 25.5 Open-Flame ddition: Ph 42.0 44.0 48.0 49.5 | 88.70 88.70 8.70 88.70 88.23 88.23 | 8.38 8.38 ace, Tappin oper, 2 oz p 7.95 | 2.32 2.32 2.32 ng Temp. 5 er 100 lb 3.29 3.29 | 0.07 0.07 2200 F 0.06 0.06 | 0.50 0.50 0.44 0.44 | 0.02 0.02 0.02 | 0.0 |
| G3- H2 H3 12 3 3 | 2150 2100 | 24120 22000 24260 23620 He 21110 21110 20200 20200 | 46230 45500 49000 46730 at No. A81, Ladle A 45730 45730 45710 47220 48990 | 37.5 23.5 22.0 31.0 25.5 Open-Flame ddition: Ph 42.0 44.0 48.0 49.5 58.0 | 88.70 88.70 e, Oil-Furn osphor-Cop 88.23 | 8.38 8.38 ace, Tappin oper, 2 oz p 7.95 | 2.32 2.32 2.32 ng Temp. : | 0.07 0.07 2200 F 0.06 | 0.50 | 0.02 | 0.0 |
| G3- H2 H3 I2 I3 I3 IA IA IA IA IA IA IA IA IA IA IA IA IA | 2150 2100 | 24120 22000 24260 23620 He 21110 21110 20200 20200 20100 | 46230 45500 49000 46730 at No. A81, Ladle A 45730 45710 47220 | 37.5 23.5 22.0 31.0 25.5 Open-Flame ddition: Ph 42.0 44.0 48.0 49.5 58.0 46.5 | 88.70 88.70 8.70 88.23 88.23 88.23 | 8.38 8.38 ace, Tappin oper, 2 oz p 7.95 7.95 | 2.32 2.32 2.32 ng Temp. 3 ser 100 lb 3.29 3.29 3.29 | 0.07 0.07 2200 F 0.06 0.06 | 0.50 0.50 0.44 0.44 | 0.02 0.02 0.02 0.02 0.02 | 0.0 0.0 0.0 0.0 |
| G3+ H2 H3 12 23 3 14 14 12 12 13 12 13 13 13 13 13 13 13 13 13 13 13 13 13 | 2150 2150 2100 2050 2000 | 24120 22000 24260 23620 He 21110 21110 20200 20200 20100 20600 | 46230 45500 49000 46730 at No. A81, Ladle A 45730 45730 45710 47220 48990 46480 | 37.5 23.5 22.0 31.0 25.5 Open-Flame ddition: Ph 42.0 44.0 48.0 49.5 58.0 | 88.70 88.70 8.70 88.70 88.23 88.23 | 8.38 8.38 ace, Tappin oper, 2 oz p 7.95 | 2.32 2.32 2.32 ng Temp. 5 er 100 lb 3.29 3.29 | 0.07 0.07 2200 F 0.06 0.06 | 0.50 0.50 0.44 0.44 | 0.02 0.02 0.02 | 0.0 0.0 0.0 0.0 |
| G3- H2 H3 I2 I3 I3 IA IA I2 I3 I3 I3 I2 I3 I3 I2 I3 I3 I3 I3 I3 I3 I3 I4 I4 I4 I4 I4 I4 I4 I4 I4 I4 I4 I4 I4 | 2150 2100 2050 2000 | 24120 22000 24260 23620 He 21110 21110 20200 20200 20100 20600 20960 22110 22610 | 46230 45500 49000 46730 at No. A81, Ladle A 45730 45730 45710 47220 48990 46480 49240 | 37.5 23.5 22.0 31.0 25.5 Open-Flame ddition: Ph 42.0 44.0 48.0 49.5 58.0 46.5 50.0 | 88.70 88.70 88.70 9, Oil-Furn osphor-Cop 88.23 88.23 88.23 | 8.38 8.38 ace, Tappin oper, 2 oz p 7.95 7.95 7.95 | 2.32 2.32 2.32 2.32 3.29 3.29 3.29 3.12 | 0.07 0.07 2200 F 0.06 0.06 0.06 | 0.50 0.50 0.44 0.44 0.44 | 0.02 0.02 0.02 0.02 0.02 0.02 | 0.0 0.0 0.0 0.0 0.0 0.0 |
| G3- H2 H3 22 23 3 11 12 12 12 13 13 12 23 33 22 33 33 | 2150 2100 2050 2000 1950 | 24120 22000 24260 23620 He 21110 20200 20200 20100 20600 20960 22110 22610 22730 | 46230 45500 49000 46730 at No. A81, Ladle A 45730 45730 45710 47220 48990 46480 49240 49000 | 37.5 23.5 22.0 31.0 25.5 Open-Flame ddition: Ph 42.0 44.0 48.0 49.5 58.0 46.5 50.0 52.5 | 88.70 88.70 8.70 88.23 88.23 88.23 | 8.38 8.38 ace, Tappin oper, 2 oz p 7.95 7.95 | 2.32 2.32 2.32 ng Temp. 3 ser 100 lb 3.29 3.29 3.29 | 0.07 0.07 2200 F 0.06 0.06 | 0.50 0.50 0.44 0.44 | 0.02 0.02 0.02 0.02 0.02 | 0.0 0.0 0.0 0.0 0.0 0.0 |
| G3- H2 H3 12 13 13 14 12 12 13 13 13 13 13 13 13 13 13 13 13 13 13 | 2150 2100 2050 2000 1950 | 24120 22000 24260 23620 He 21110 21110 20200 20200 20100 20600 20960 22110 22610 22730 23620 | 46230 45500 49000 46730 at No. A81, Ladle A 45730 45730 45710 47220 48990 46480 49240 49000 48740 48990 47490 | 37.5 23.5 22.0 31.0 25.5 Open-Flame ddition: Ph 42.0 44.0 48.0 49.5 58.0 46.5 50.0 52.5 38.5 39.0 | 88.70 88.70 88.70 9, Oil-Furn osphor-Cop 88.23 88.23 88.23 | 8.38 8.38 ace, Tappin oper, 2 oz p 7.95 7.95 7.95 | 2.32 2.32 2.32 2.32 2.32 2.32 3.29 3.29 | 0.07 0.07 2200 F 0.06 0.06 0.06 0.06 | 0.50 0.50 0.44 0.44 0.44 0.45 | 0.02 0.02 0.02 0.02 0.02 0.02 | 0.00 0.00 0.00 0.00 0.00 0.00 |
| G3- H2 H3 I2 I3 I3 IA I2 I3 I3 I3 I3 I3 I3 I3 I3 I3 I3 I3 I3 I3 | 2150 2100 2050 2000 1950 | 24120 22000 24260 23620 He 21110 20200 20200 20100 20600 20960 22110 22610 22730 | 46230 45500 49000 46730 at No. A81, Ladle A 45730 45710 47220 48990 46480 49240 49000 48740 48990 48990 | 37.5 23.5 22.0 31.0 25.5 Open-Flame ddition: Ph 42.0 44.0 48.0 49.5 58.0 46.5 50.0 52.5 38.5 39.0 | 88.70 88.70 8.70 88.70 88.23 88.23 88.23 88.22 88.22 | 8.38 8.38 ace, Tappin oper, 2 oz p 7.95 7.95 7.95 8.12 8.12 | 2.32 2.32 2.32 2.32 3.29 3.29 3.29 3.12 | 0.07 0.07 2200 F 0.06 0.06 0.06 | 0.50 0.50 0.44 0.44 0.44 | 0.02 0.02 0.02 0.02 0.02 0.02 | 0.00 0.00 0.00 0.00 0.00 |

Table 1 (cont.)—Physical and Chemical Properties of Gun Metal as Affected by Melting Unit, Tapping and Casting Temperatures and Deoxidation

| Test Bar No. | Casting Temp. °F | Yield Point, | | Elongation, | | | | | | | |
|-----------------|---------------------|--------------|-------------|-----------------|-----------------------|--------------|--------------|----------|------|------|---|
| No. | Temp. °F | nsi | | | | | | | | | |
| | | Por | psi | % in 2 in. | Cu | Sn | Zn | Pb | Ni | Fe | |
| | | H | | 105, Open-Flai | | | | 2100 F | | | |
| | | | | le Addition: P | - | | per 100 lb | | | | |
| A2 | 2050 | 25000 | 48000 | 52.5 | 88.23 | 7.74 | 3.42 | 0.11 | 0.49 | 0.01 | (|
| A3 | | 23620 | 48990 | 57.5 | | | | | | | |
| B2 | 2000 | 23750 | 48750 | 50.0 | 88.23 | 7.74 | 3.42 | 0.11 | 0.49 | 0.01 | (|
| B3 | | Defective | Bar | | | | | | | | |
| C2 | 1950 | 27890 | 49750 | 37.0 | 88.23 | 7.74 | 3.42 | 0.11 | 0.49 | 0.01 | 1 |
| C3 | | 29250 | 51000 | 44.0 | | | | | | | |
| D2 | 1900 | 30400 | 49750 | 29.0 | 88.23 | 8.02 | 3.11 | 0.14 | 0.49 | 0.01 | |
| D3 | | 28750 | 49750 | 32.0 | | | | | | | |
| E2 | 1850 | 29500 | 45750 | 22.0 | 88.23 | 8.02 | 3.11 | 0.14 | 0.49 | 0.01 | |
| E3 | | 27220 | 43060 | 19.0 | | | | | | | |
| | | н | eat No. A9 | 2, Indirect-Arc | | | ping Temp | . 2400 F | | | |
| | | | | Lad | le Addition | n: None | | | | | |
| A2 | 2350 | 18180 | 30810 | 17.0 | 87.02 | 7.96 | 3.93 | 0.15 | 0.91 | 0.02 | |
| A3 | | 20100 | 31160 | 16.0 | 10 | | | | | | |
| B2 | 2300 | 21100 | 41960 | 35.0 | 87.15 | 8.10 | 3.65 | 0.13 | 0.94 | 0.02 | (|
| B3 | | 20750 | 43000 | 38.5 | | | | | | | |
| C2 | 2250 | 21100 | 40200 | 30.0 | 87.02 | 7.96 | 3.93 | 0.15 | 0.91 | 0.02 | (|
| C3 | | 21100 | 39950 | 26.5 | | | | | | | |
| D2 | 2200 | 21210 | 42930 | 33.5 | 87.15 | 8.10 | 3.65 | 0.13 | 0.94 | 0.02 | (|
| D3 | | 21210 | 43180 | 39.0 | | | | | | | |
| E2 | 2150 | 22000 | 44750 | 38.0 | 87.02 | 7.96 | 3.93 | 0.15 | 0.91 | 0.02 | (|
| E3 | | 21720 | 44750 | 37.5 | 01104 | | | | | | |
| F2 | 2100 | 22000 | 45000 | 41.5 | 87.15 | 8.10 | 3.65 | 0.13 | 0.94 | 0.02 | (|
| F3 | 4100 | 21610 | 47490 | 49.5 | 01.10 | 0.40 | 0.00 | 0120 | 0.01 | 0.02 | |
| G2 | 2050 | 22750 | 47500 | 43.5 | 87.02 | 7.96 | 3.93 | 0.15 | 0.91 | 0.02 | (|
| G3 | 4030 | 23120 | 49250 | 51.5 | 07.04 | 7.50 | 3.33 | 0.10 | 0.51 | 0.04 | , |
| H2 | 2000 | 24000 | 50500 | 45.5 | 87.15 | 8.10 | 3.93 | 0.15 | 0.91 | 0.02 | |
| H3 | 2000 | 24120 | 50250 | 39.0 | 07.13 | 0.10 | 3.33 | 0.13 | 0.31 | 0.04 | , |
| 12 | 1050 | | | | 07 00 | 0 17 | 9 56 | 0.19 | 0.90 | 0.02 | (|
| | 1950 | 25130 | 50250 | 38.0 | 87.22 | 8.17 | 3.56 | 0.12 | 0.89 | 0.02 | , |
| 13 | 1000 | 24750 | 48750 | 32.0 | 07.00 | 0.15 | 0 40 | 0.10 | 0.00 | 0.00 | , |
| J2 | 1900 | 26630 | 48740 | 26.5 | 87.22 | 8.17 | 3.56 | 0.12 | 0.89 | 0.02 | (|
| J3 | *0*0 | 26130 | 41210 | 15.0 | | | | 0.10 | 0.00 | 0.00 | |
| K2 | 1850 | 26380 | 48490 | 24.5 | 87.22 | 8.17 | 3.56 | 0.12 | 0.89 | 0.02 | 0 |
| К3 | | 26630 | 41200 | 14.5 | | | | | | | |
| | | Не | eat A93, Ir | direct-Arc Ele | ectric Furre Addition | | ng Temp. | 2200 F | | | |
| A2 | 2150 | 22230 | 42890 | 34.0 | 87.03 | 8.15 | 3.79 | 0.15 | 0.85 | 0.02 | 0 |
| A3 | 4130 | 22110 | 43720 | 35.0 | 07.00 | 0.13 | 3.19 | 0.13 | 0.00 | 0.02 | , |
| B2 | 2100 | 22120 | 44470 | 40.0 | 0 6 | 8.06 | 3.26 | 0.15 | 0.85 | 0.02 | (|
| B3 | 2100 | | | | 87.65 | 8.00 | 3.40 | 0.13 | 0.00 | 0.04 | |
| | 9050 | 23500 | 44250 | 35.0 | 07 09 | 0 1 5 | 9 70 | 0.15 | 0.05 | 0.09 | |
| C2 | 2050 | 23250 | 46500 | 41.5 | 87.03 | 8.15 | 3.79 | 0.15 | 0.85 | 0.02 | (|
| C3 | 2000 | 23120 | 47740 | 48.5 | OF C* | 9.00 | 9.00 | 0.25 | 0.05 | 0.00 | |
| D2 | 2000 | 24620 | 45730 | 33.0 | 87.65 | 8.06 | 3.26 | 0.15 | 0.85 | 0.02 | (|
| D3 | 1000 | 25130 | 49250 | 38.0 | 02.00 | 0.25 | 9 20 | 0.75 | 0.02 | 0.00 | |
| E2 | 1950 | 25630 | 48990 | 30.0 | 87.03 | 8.15 | 3.79 | 0.15 | 0.85 | 0.02 | (|
| E3 | | 26130 | 47990 | 28.0 | | | | | 0.57 | 0.00 | |
| F2 | 1900 | 27140 | 41210 | 12.0 | 87.65 | 8.06 | 3.26 | 0.15 | 0.85 | 0.02 | C |
| F3 | | 28140 | 46230 | 18.0 | | | | | | | |
| G2 | 1850 | 27140 | 43220 | 14.0 | 87.03 | 8.15 | 3.79 | 0.15 | 0.85 | 0.02 | 0 |
| G3 | | 26500 | 45250 | 19.5 | | | | | | | |
| | | He | at No. A10 | 2, Indirect-Arc | | | ping Temp. | 2100 F | | | |
| 19 | 9050 | 99750 | 47000 | | Addition | | 9.40 | 0.19 | 0.46 | 0.01 | 0 |
| A2 | 2050 | 23750 | 47000 | 44.0 | 88.01 | 7.90 | 3.49 | 0.13 | 0.46 | 0.01 | U |
| A3 | 0000 | 23670 | 47990 | 53.5 | 00.01 | m 00 | 9.40 | 0.10 | 0.40 | 0.01 | |
| B2 | 2000 | 24000 | 47750 | 45.0 | 88.01 | 7.90 | 3.49 | 0.13 | 0.46 | 0.01 | 0 |
| B3 | 1000 | 23750 | 49500 | 43.0 | 00.01 | | 9 10 | 0.00 | 0.40 | 0.01 | - |
| | 1950 | 26000 | 46500 | 29.0 | 88.01 | 7.90 | 3.49 | 0.13 | 0.46 | 0.01 | 0 |
| C2 | | | | | | | | | | | |
| C2 C3 | 1000 | 26500 | 47750 | 31.0 | | | | | 0.15 | 0.61 | _ |
| C2 C3 D2 | 1900 | 29400 | 42310 | 15.0 | 87.74 | 8.19 | 3.47 | 0.13 | 0.46 | 0.01 | 0 |
| C2 C3 | 1900 1850 | | | | 87.74 87.74 | 8.19 8.19 | 3.47 3.47 | 0.13 | 0.46 | 0.01 | 0 |

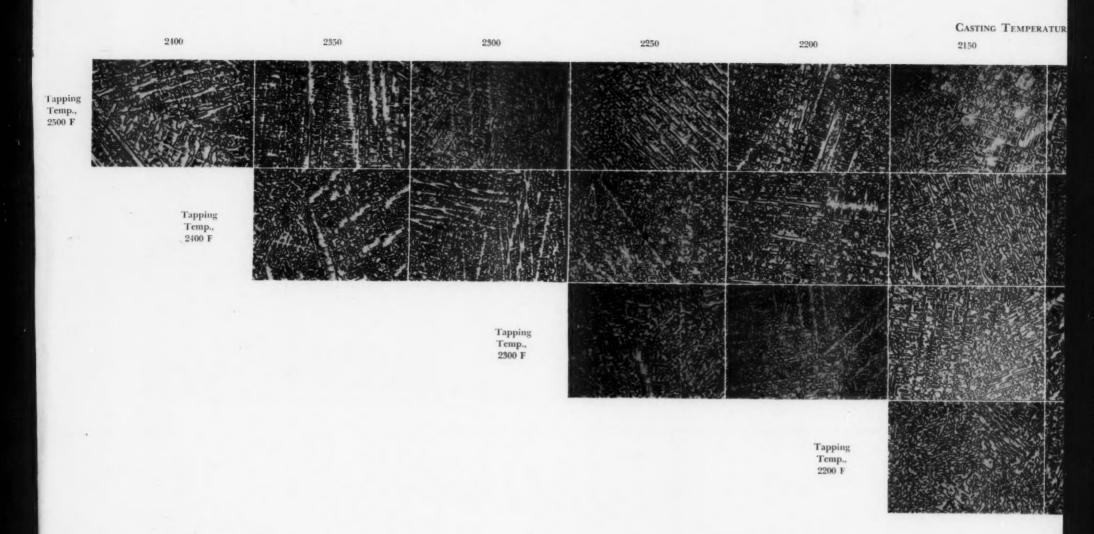
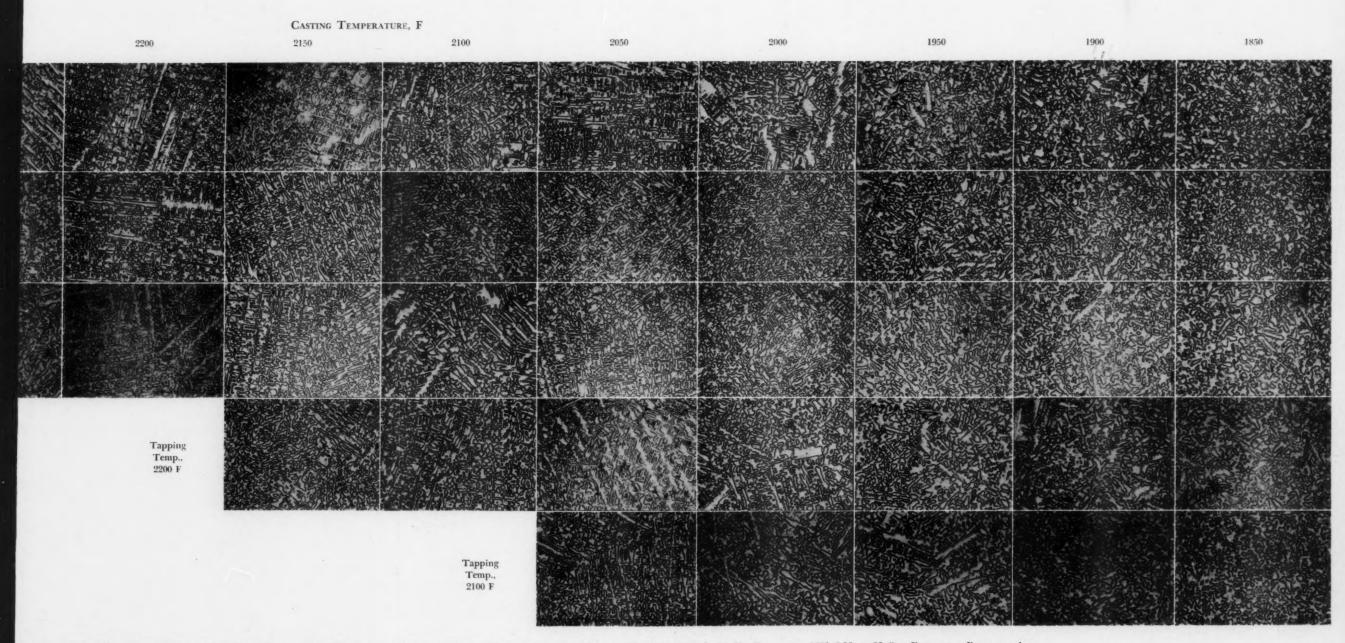


Fig. 12-Effect of Tapping and Casting Temperatures on the Microstructure of Transverse Sections of Gun Metal Test Bars. 2 oz/



OSTRUCTURE OF TRANSVERSE SECTIONS OF GUN METAL TEST BARS. 2 OZ/100 LB PHOSPHOR-COPPER ADDED TO LADLE. MAGNIFICATION: 25 X. ETCHANT: NH4OH + H2O2. FURNACE: ELECTRIC ARC.

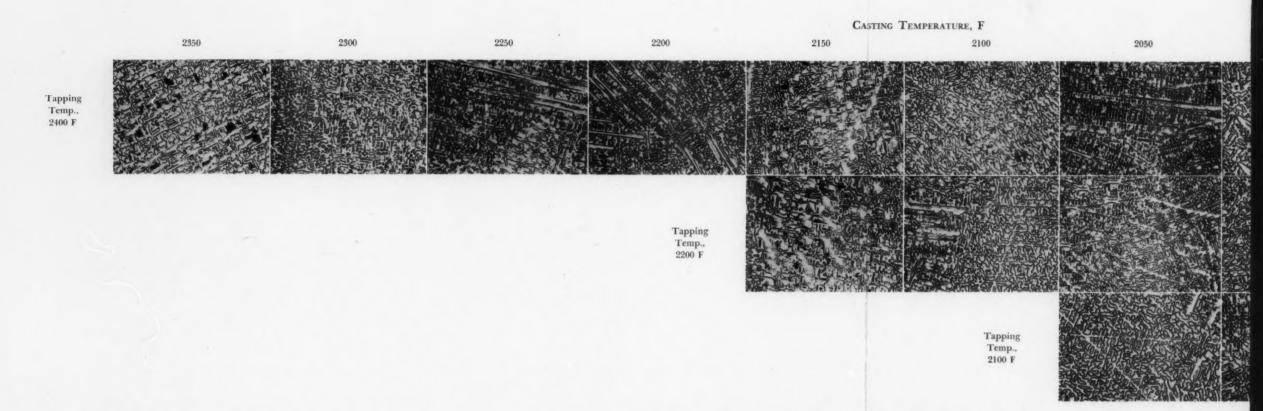
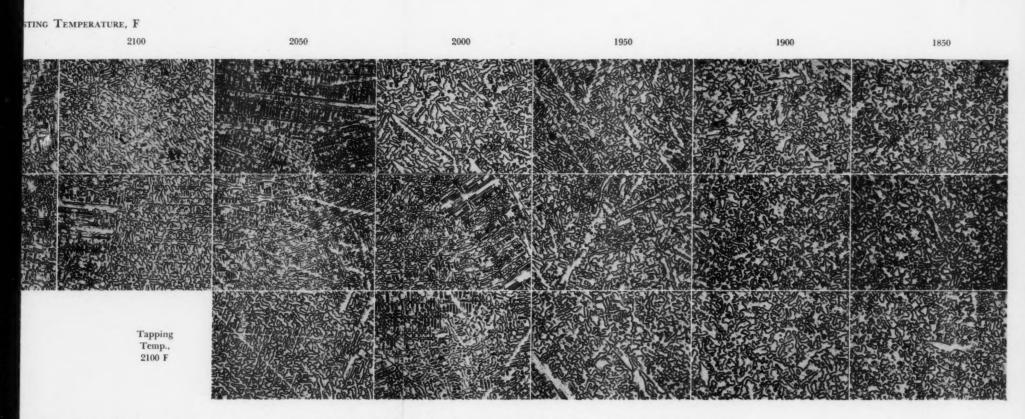


FIG. 13-EFFECT OF TAPPING AND CASTING TEMPERATURES ON THE MICROSTRUCTURE OF TRANSVERSE SECTIONS OF GUN METAL TEST BARS. NO PHOSPHOR-COPPER ADDED TO LADLE. MAGNIFICATION



TAL TEST BARS. NO PHOSPHOR-COPPER ADDED TO LADLE. MAGNIFICATION: X 25. ETCHANT: NH4OH + H2O2. FURNACE: ELECTRIC ARC.

rep not dec tem 200 ing stre 235

Table 1 (cont.) —Physical and Chemical Properties of Gun Metal as Affected by Melting Unit, Tapping and Casting Temperatures and Deoxidation

| | | Phys | Tensile | ties | | | Chemi | cal Analyse | es, Per Cent | | |
|--|--|--|---|--|--|---|---|--|--|--|--|
| Test Bar No. | Casting Temp. °F | Yield Point, psi | Strength, psi | Elongation, % in 2 in. | Cu | Sn | Zn | Pb | Ni | Fe | |
| | | Н | leat No. As | 5, Open-Flame Ladle | Oil Furi | | Temp. | 2400 F | | | |
| A2 | 2350 | 23870 | 42710 | 37.0 | 89.97 | 7.49 | 1.64 | 0.09 | 0.80 | 0.01 | . 0 |
| A3 | | 24120 | 43220 | 35.0 | | | | | | | |
| B2 | 2300 | 23370 | 39950 | 29.0 | 88.90 | 7.71 | 2.47 | 0.11 | 0.80 | 0.01 | (|
| B3 | 0056 | 23620 | 38190 | 23.5 | | | | | | | |
| C2 C3 | 2250 - | 24120 | 45230 | 42.5 | 88.31 | 7.77 | 2.99 | 0.12 | 0.80 | 0.01 | (|
| D2 | 2200 | 23500 | 44000 | 38.5 | 00.00 | P P 3 | 0.45 | 0.11 | 0.00 | 0.01 | |
| D3 | 2200 | 24370 24120 | 46990 43720 | 50.0 35.5 | 88.90 | 7.71 | 2.47 | 0.11 | 0.80 | 0.01 | (|
| E2 | 2150 | 24750 | 45960 | 38.0 | 88.31 | 7.77 | 2.99 | 0.12 | 0.80 | 0.01 | (|
| E3 | 2130 | 24750 | 46970 | 45.5 | 00.31 | 1.11 | 2.99 | 0.12 | 0.80 | 0.01 | , |
| F2 | 2100 | 25130 | 46480 | 41.0 | 88.90 | 7.71 | 2.47 | 0.11 | 0.80 | 0.01 | (|
| F3 | 2100 | 24750 | 46970 | 47.0 | 00.30 | 1.11 | 4.41 | 0.11 | 0.00 | 0.01 | |
| G2 | 2050 | 22360 | 48740 | 54.0 | 88.31 | 7.77 | 2.99 | 0.12 | 0.80 | 0.01 | (|
| G3 | 2000 | 23500 | 47750 | 43.0 | 00.31 | 1.11 | 4.33 | 0.12 | 0.00 | 0.01 | , |
| H2 | 2000 | 24120 | 51000 | 44.0 | 88.90 | 7.71 | 2.47 | 0.11 | 0.80 | 0.01 | (|
| НЗ | 2000 | 22860 | 50250 | 51.0 | 00.90 | 7.71 | 4.11 | 0.11 | 0.00 | 0.01 | , |
| 12 | 1950 | 25380 | 46990 | 26.0 | 89.97 | 7.49 | 1.64 | 0.09 | 0.80 | 0.01 | (|
| 13 | 2000 | 23870 | 40200 | 15.0 | 03.31 | 2.10 | 1.01 | 0.05 | 0.00 | 0.01 | , |
| 12 | 1900 | 25130 | 41460 | 16.0 | 89.97 | 7.49 | 1.64 | 0.09 | 0.80 | 0.01 | 0 |
| J3 | | 25510 | 43430 | 18.0 | 00.01 | | 1.01 | 0.00 | 0.00 | 0.01 | , |
| K2 | 1850 | 25130 | 47740 | 27.0 | 90.62 | 7.80 | .65 | 0.12 | 0.80 | 0.01 | (|
| К3 | | 23870 | 41710 | 15.0 | 50104 | 7.00 | .00 | 0.11 | -7.00 | 0.01 | , |
| A2 | 2150 | 23000 | 47000 | | 87.67 | 7.90 | 3.55 | 0.11 | 0.76 | 0.01 | 0 |
| A3 | | 22860 | 46480 | 40.5 | | | | | | | |
| B2 | 2100 | and the contract of the contra | | 45.0 | 89.15 | | | | | | |
| | 4.00 | 23000 | 45250 | | 03.43 | 7.72 | 2.25 | 0.13 | 0.74 | 0.01 | 0 |
| B3 | | 23230 | 46470 | 43.5 | | | | | | | |
| B3 C2 | 2050 | 23230 23370 | 46470 47740 | 43.5 48.5 | 87.67 | | 3.55 | 0.13 | 0.74 | 0.01 | |
| B3 C2 C3 | 2050 | 23230 23370 23990 | 46470 47740 45460 | 43.5 48.5 36.5 | 87.67 | 7.90 | 3.55 | 0.11 | 0.76 | 0.01 | 0 |
| B3 C2 C3 D2 | | 23230 23370 23990 25130 | 46470 47740 45460 47740 | 43.5 48.5 36.5 33.5 | | 7.90 | | | | | 0 |
| B3 C2 C3 D2 D3 | 2050 2000 | 23230 23370 23990 25130 24120 | 46470 47740 45460 47740 48240 | 43.5 48.5 36.5 33.5 41.0 | 87.67 87.67 | 7.90 7.90 | 3.55 3.55 | 0.11 | 0.76 0.76 | 0.01 | 0 |
| B3 C2 C3 D2 D3 E2 | 2050 | 23230 23370 23990 25130 24120 26630 | 46470 47740 45460 47740 48240 49250 | 43.5 48.5 36.5 33.5 41.0 29.0 | 87.67 | 7.90 7.90 | 3.55 | 0.11 | 0.76 | 0.01 | 0 |
| B3 C2 C3 D2 D3 E2 E3 | 2050 2000 1950 | 23230 23370 23990 25130 24120 26630 26130 | 46470 47740 45460 47740 48240 49250 47240 | 43.5 48.5 36.5 33.5 41.0 29.0 25.5 | 87.67 87.67 | 7.90 7.90 7.90 | 3.55 3.55 3.55 | 0.11 0.11 0.11 | 0.76 0.76 0.76 | 0.01 0.01 0.01 | 0 |
| B3 C2 C3 D2 D3 E2 E3 F2 | 2050 2000 | 23230 23370 23990 25130 24120 26630 26130 26260 | 46470 47740 45460 47740 48240 49250 47240 41670 | 43.5 48.5 36.5 33.5 41.0 29.0 25.5 17.0 | 87.67 87.67 | 7.90 7.90 7.90 | 3.55 3.55 | 0.11 | 0.76 0.76 | 0.01 | 0 |
| B3 C2 C3 D2 D3 E2 E3 F2 F3 | 2050 2000 1950 1900 | 23230 23370 23990 25130 24120 26630 26130 26260 26770 | 46470 47740 45460 47740 48240 49250 47240 41670 43430 | 43.5 48.5 36.5 33.5 41.0 29.0 25.5 17.0 19.0 | 87.67 87.67 87.67 87.85 | 7.90 7.90 7.90 8.01 | 3.55 3.55 3.55 3.25 | 0.11 0.11 0.11 0.12 | 0.76 0.76 0.76 0.76 | 0.01 0.01 0.01 0.01 | 0 0 0 |
| B3 C2 C3 D2 D3 E2 E3 F2 F3 G2 | 2050 2000 1950 | 23230 23370 23990 25130 24120 26630 26130 26260 26770 27500 | 46470 47740 45460 47740 48240 49250 47240 41670 43430 45000 | 43.5 48.5 36.5 33.5 41.0 29.0 25.5 17.0 19.0 | 87.67 87.67 | 7.90 7.90 7.90 8.01 | 3.55 3.55 3.55 | 0.11 0.11 0.11 | 0.76 0.76 0.76 | 0.01 0.01 0.01 | 0 0 0 |
| B3 C2 C3 D2 D3 E2 E3 F2 F3 | 2050 2000 1950 1900 | 23230 23370 23990 25130 24120 26630 26130 26260 26770 27500 27000 | 46470 47740 45460 47740 48240 49250 47240 41670 43430 45000 40750 | 43.5 48.5 36.5 33.5 41.0 29.0 25.5 17.0 19.0 21.0 | 87.67 87.67 87.67 87.85 87.85 | 7.90 7.90 7.90 8.01 8.01 | 3.55 3.55 3.55 3.25 3.25 | 0.11 0.11 0.11 0.12 0.12 | 0.76 0.76 0.76 0.76 | 0.01 0.01 0.01 0.01 | 0 0 0 |
| B3 C2 C3 D2 D3 E2 E3 F2 F3 G2 G3 | 2050 2000 1950 1900 1850 | 23230 23370 23390 25130 24120 26630 26130 26260 26770 27500 27000 | 46470 47740 45460 47740 48240 49250 47240 41670 43430 45000 40750 | 43.5 48.5 36.5 33.5 41.0 29.0 25.5 17.0 19.0 21.0 15.5 | 87.67 87.67 87.85 87.85 Oil Furn Addition: | 7.90 7.90 7.90 8.01 8.01 None | 3.55 3.55 3.55 3.25 3.25 Temp. | 0.11 0.11 0.11 0.12 0.12 | 0.76 0.76 0.76 0.76 0.76 | 0.01 0.01 0.01 0.01 0.01 | 0 0 0 0 |
| B3 C2 C3 D2 D3 E2 E3 F2 F3 G2 G3 | 2050 2000 1950 1900 | 23230 23370 23990 25130 24120 26630 26130 26260 26770 27500 27000 | 46470 47740 45460 47740 48240 49250 47240 41670 43430 45000 40750 eat No. A10 | 43.5 48.5 36.5 33.5 41.0 29.0 25.5 17.0 19.0 21.0 15.5 4, Open Flame Ladle | 87.67 87.67 87.67 87.85 87.85 Oil Furn | 7.90 7.90 7.90 8.01 8.01 None | 3.55 3.55 3.55 3.25 3.25 | 0.11 0.11 0.11 0.12 0.12 | 0.76 0.76 0.76 0.76 | 0.01 0.01 0.01 0.01 | 0 0 0 0 |
| B3 C2 C3 D2 D3 E2 E3 F2 F3 G2 G3 | 2050 2000 1950 1900 1850 | 23230 23370 23990 25130 24120 26630 26130 26260 26770 27500 27000 | 46470 47740 45460 47740 48240 49250 47240 41670 43430 45000 40750 eat No. A10 | 43.5 48.5 36.5 33.5 41.0 29.0 25.5 17.0 19.0 21.0 15.5 4, Open Flame Ladle | 87.67 87.67 87.67 87.85 87.85 Oil Furn Addition: | 7.90 7.90 7.90 8.01 8.01 ace, Tapping None 7.89 | 3.55 3.55 3.55 3.25 3.25 Temp. : | 0.11 0.11 0.12 0.12 0.12 2100 F 0.13 | 0.76 0.76 0.76 0.76 0.76 | 0.01 0.01 0.01 0.01 0.01 | 0 0 0 0 |
| B3 C2 C3 D2 D3 E2 E3 F2 F3 G2 G3 | 2050 2000 1950 1900 1850 | 23230 23370 23390 25130 24120 26630 26130 26260 26770 27500 27000 He | 46470 47740 45460 47740 48240 49250 47240 41670 43430 45000 40750 eat No. A10 48320 47240 49250 | 43.5 48.5 36.5 33.5 41.0 29.0 25.5 17.0 19.0 21.0 15.5 4, Open Flame Ladle 55.5 51.0 | 87.67 87.67 87.85 87.85 Oil Furn Addition: | 7.90 7.90 7.90 8.01 8.01 ace, Tapping None 7.89 | 3.55 3.55 3.55 3.25 3.25 Temp. | 0.11 0.11 0.11 0.12 0.12 | 0.76 0.76 0.76 0.76 0.76 | 0.01 0.01 0.01 0.01 0.01 | 0 0 0 0 |
| B3 C2 C3 D2 D3 E2 E3 F2 F3 G2 G3 | 2050 2000 1950 1900 1850 2050 2000 | 23230 23370 23370 23390 25130 24120 26630 26130 26260 26770 27500 27000 He 22470 23620 25880 26130 | 46470 47740 45460 47740 48240 49250 47240 41670 43430 45000 40750 eat No. A10 48320 47240 49250 48990 | 43.5 48.5 36.5 33.5 41.0 29.0 25.5 17.0 19.0 21.0 15.5 4, Open Flame Ladle 55.5 51.0 48.0 | 87.67 87.67 87.85 87.85 Oil Furn Addition: 88.40 | 7.90 7.90 7.90 8.01 8.01 8.01 None 7.89 8.00 | 3.55 3.55 3.55 3.25 3.25 Temp. : | 0.11 0.11 0.12 0.12 0.12 2100 F 0.13 0.14 | 0.76 0.76 0.76 0.76 0.76 0.49 | 0.01 0.01 0.01 0.01 0.01 | 0 0 0 0 0 0 0 0 |
| B3 C2 C3 D2 D3 E2 E3 F2 F3 G2 G3 A2 A3 B2 B3 C2 | 2050 2000 1950 1900 1850 | 23230 23370 23390 25130 24120 26630 26130 26260 26770 27500 27000 He 22470 23620 25880 26130 26380 | 46470 47740 45460 47740 48240 49250 47240 41670 43430 40750 tat No. A10 48320 47240 49250 48990 49750 | 43.5 48.5 36.5 33.5 41.0 29.0 25.5 17.0 19.0 21.0 15.5 4. Open Flame Ladle 55.5 51.0 48.0 44.0 44.5 | 87.67 87.67 87.67 87.85 87.85 Oil Furn Addition: | 7.90 7.90 7.90 8.01 8.01 8.01 None 7.89 8.00 | 3.55 3.55 3.55 3.25 3.25 Temp. : | 0.11 0.11 0.12 0.12 0.12 2100 F 0.13 | 0.76 0.76 0.76 0.76 0.76 | 0.01 0.01 0.01 0.01 0.01 | 0 0 0 0 0 0 0 0 |
| B3 C2 C3 D2 D3 E2 E3 F2 F3 G2 G3 A2 A3 B2 B3 C2 C3 | 2050 2000 1950 1900 1850 2050 2000 1950 | 23230 23370 23990 25130 24120 26630 26130 26260 26770 27500 27000 He 22470 23620 25880 26130 26380 24500 | 46470 47740 45460 47740 48240 49250 47240 41670 43430 45000 40750 eat No. A10 48320 47240 49250 48990 49750 48500 | 43.5 48.5 36.5 33.5 41.0 29.0 25.5 17.0 19.0 21.0 15.5 4. Open Flame Ladle 55.5 51.0 48.0 44.0 44.5 39.0 | 87.67 87.67 87.85 87.85 Oil Furn Addition: 88.40 87.80 | 7.90 7.90 7.90 8.01 8.01 8.01 7.89 8.00 8.00 | 3.55 3.55 3.55 3.25 3.25 Temp. 1 | 0.11 0.11 0.12 0.12 0.12 2100 F 0.13 0.14 0.14 | 0.76 0.76 0.76 0.76 0.76 0.49 0.49 | 0.01 0.01 0.01 0.01 0.01 0.01 | 000000000000000000000000000000000000000 |
| B3 C2 C3 D2 D3 E2 E3 F2 F3 G2 G2 G3 A2 A3 B2 B3 C2 C3 D2 | 2050 2000 1950 1900 1850 2050 2000 | 23230 23370 23990 25130 24120 26630 26130 26260 26770 27500 27000 He 22470 23620 25880 26130 26380 24500 27270 | 46470 47740 45460 47740 48240 49250 47240 41670 43430 45000 40750 eat No. A10 48320 47240 49250 48990 49750 48500 42680 | 43.5 48.5 36.5 33.5 41.0 29.0 25.5 17.0 19.0 21.0 15.5 4, Open Flame Ladle 55.5 51.0 48.0 44.5 39.0 17.0 8 | 87.67 87.67 87.85 87.85 Oil Furn Addition: 88.40 | 7.90 7.90 7.90 8.01 8.01 8.01 7.89 8.00 8.00 | 3.55 3.55 3.55 3.25 3.25 Temp. : | 0.11 0.11 0.12 0.12 0.12 2100 F 0.13 0.14 | 0.76 0.76 0.76 0.76 0.76 0.49 | 0.01 0.01 0.01 0.01 0.01 | 000000000000000000000000000000000000000 |
| B3 C2 C3 D2 D3 E2 E3 F2 F3 G2 G3 A2 A3 B2 B3 C2 C3 D2 D3 | 2050 2000 1950 1900 1850 2050 2000 1950 1900 | 23230 23370 23390 25130 24120 26630 26130 26260 26770 27500 27000 Htc 22470 23620 25880 26130 26380 24500 27270 27500 | 46470 47740 45460 47740 48240 49250 47240 41670 43430 45000 40750 at No. A10 48320 47240 49250 48990 49750 48500 42680 47190 | 43.5 48.5 36.5 33.5 41.0 29.0 25.5 17.0 19.0 21.0 15.5 4, Open Flame Ladle 55.5 51.0 48.0 44.0 44.5 39.0 17.0 26.0 | 87.67 87.67 87.85 87.85 87.85 Oil Furn Addition: 88.40 87.80 | 7.90 7.90 7.90 8.01 8.01 8.01 8.00 8.00 8.00 8.00 | 3.55 3.55 3.55 3.25 3.25 Temp. : 3.08 3.56 3.56 | 0.11 0.11 0.12 0.12 0.12 2100 F 0.13 0.14 0.14 | 0.76 0.76 0.76 0.76 0.76 0.49 0.49 0.49 | 0.01 0.01 0.01 0.01 0.01 0.01 0.01 0.01 | 0.000 |
| B3 C2 C3 D2 D3 E2 E3 F2 F3 G2 G3 B2 C3 D2 | 2050 2000 1950 1900 1850 2050 2000 1950 | 23230 23370 23990 25130 24120 26630 26130 26260 26770 27500 27000 He 22470 23620 25880 26130 26380 24500 27270 | 46470 47740 45460 47740 48240 49250 47240 41670 43430 45000 40750 eat No. A10 48320 47240 49250 48990 49750 48500 42680 | 43.5 48.5 36.5 33.5 41.0 29.0 25.5 17.0 19.0 21.0 15.5 4, Open Flame Ladle 55.5 51.0 48.0 44.0 44.5 39.0 17.0 26.0 | 87.67 87.67 87.85 87.85 Oil Furn Addition: 88.40 87.80 | 7.90 7.90 7.90 8.01 8.01 8.01 8.00 8.00 8.00 8.00 | 3.55 3.55 3.55 3.25 3.25 Temp. 1 | 0.11 0.11 0.12 0.12 0.12 2100 F 0.13 0.14 0.14 | 0.76 0.76 0.76 0.76 0.76 0.49 0.49 | 0.01 0.01 0.01 0.01 0.01 0.01 | 0 0 0 0 0 0 0 0 0 0 |

represents one tapping temperature value. It will be noted that the ultimate strength curves increase with decreasing pouring temperature, regardless of tapping temperature, reaching their peak values at 1950 and 2000 F pouring temperatures. Below the 1950 F casting temperature a downward trend in the ultimate strength curves is noted. Casting temperatures of 2400, 2350 and 2300 F for the 2500 F superheat run; 2350

and 2300 F for the 2400 F heat; and 1850 F for the 2200 F heat, yielded ultimate strength values which were below specification requirements for gun metal. The elongation curves follow the same trend as the ultimate strength curves, i.e. increasing with decreasing casting temperatures, regardless of tapping temperature, reaching their peak values to 2050 F casting temperature and from that point decreasing rather

TABLE 2-CHEMICAL ANALYSES OF GUN METAL TEST CYLINDER

| Tapping | | | | | | Casting | | | , | | | Tapping |
|---------|-------|--------|------|-----------|------|---------|-------|------|------------|------|------|---------|
| Temp. | Cu | Sn | Zn | Pb | Ni | Temp. | Cu | Sn | Zn | Pb | Ni | Temp. |
| | | | | Phosphor- | | | | | sphor-Copp | | | |
| | | | | to Ladle | | | | | d to Ladle | | | |
| | 89.30 | 7.83 | 2.27 | 0.14 | 0.54 | 2300 | 89.22 | 7.88 | 2.13 | 0.22 | 0.52 | |
| | 89.30 | 7.83 | 2.27 | 0.14 | 0.54 | 2200 | 89.22 | 7.88 | 2.13 | 0.22 | 0.52 | |
| 2500 | 89.68 | 7.94 | 1.66 | 0.13 | 0.56 | 2100 | 89.39 | 7.96 | 1.91 | 0.15 | 0.56 | 2500 |
| | 89.98 | 7.55 | 1.80 | 0.14 | 0.52 | 2050 | 89.95 | 7.59 | 1.76 | 0.17 | 0.52 | |
| | 89.98 | 7.55 | 1.80 | 0.14 | 0.52 | 1950 | 89.95 | 7.59 | 1.76 | 0.17 | 0.52 | |
| | 88.60 | 7.79 | 2.86 | 0.28 | 0.45 | 2300 | 87.68 | 7.88 | 3.62 | 0.18 | 0.63 | |
| | 88.60 | . 7.79 | 2.86 | 0.28 | 0.45 | 2200 | 87.68 | 7.88 | 3.62 | 0.18 | 0.63 | |
| 2400 | 88.50 | 8.06 | 2.77 | 0.24 | 0.41 | 2100 | 88.92 | 7.11 | 3.32 | 0.18 | 0.45 | 2400 |
| | 88.50 | 8.06 | 2.77 | 0.24 | 0.41 | 2050 | 88.92 | 7.11 | 3.32 | 0.18 | 0.45 | |
| | 89.15 | 7.80 | 2.36 | 0.24 | 0.43 | 1950 | 89.47 | 7.61 | 2.28 | 0.19 | 0.44 | |
| | 87.84 | 8.06 | 3.23 | 0.17 | 0.68 | 2200 | 87.90 | 7.59 | 3.61 | 0.22 | 0.66 | |
| 2300 | 87.84 | 8.06 | 3.23 | 0.17 | 0.68 | 2100 | 87.90 | 7.59 | 3.61 | 0.22 | 0.66 | 2300 |
| | 88.30 | 8.14 | 2.70 | 0.19 | 0.65 | 2050 | 88.43 | 7.47 | 3.25 | 0.18 | 0.65 | |
| | 88.30 | 8.14 | 2.70 | 0.19 | 0.65 | 1950 | 88.43 | 7.47 | 3.25 | 0.18 | 0.65 | |
| | 87.80 | 7.95 | 3.31 | 0.14 | 0.78 | 2100 | 88.41 | 7.88 | 3.03 | 0.23 | 0.44 | |
| 2200 | 88.48 | 7.82 | 2.73 | 0.19 | 0.76 | 2050 | 88.15 | 7.89 | 3.10 | 0.18 | 0.66 | 2200 |
| | 88.48 | 7.82 | 2.73 | 0.19 | 0.76 | 1950 | 88.15 | 7.89 | 3.10 | 0.18 | 0.66 | |
| 2100 | 87.90 | 8.14 | 3.33 | 0.17 | 0.44 | 2050 | 87.62 | 7.94 | 3.78 | 0.19 | 0.45 | 2100 |
| | 87.90 | 8.14 | 3.33 | 0.17 | 0.44 | 1950 | 87.62 | 7.94 | 3.78 | 0.19 | 0.45 | |

TABLE 3-CHEMICAL ANALYSES OF GUN METAL TEST CYLINDER

| Tapping Temp. °F | Cu | Sn | Zn | Pb | Ni | Casting Temp. °F | Cu | Sn | Zn | Pb | Ni | Tapping Temp. °F |
|------------------------|-------|------|---------|-----------|------|------------------------|-------|------|--------------------|------|------|------------------------|
| | | | | Phosphor- | | | | | sphor-Cop | | | |
| | 87.63 | 8.25 | Added t | 0.19 | 0.54 | 2300 | 87.97 | 8.13 | d to Ladle 3.24 | 0.17 | 0.48 | |
| | 87.74 | 8.21 | 3.36 | 0.11 | 0.57 | 2200 | 88.02 | 7.99 | 3.32 | 0.18 | 0.48 | |
| 2500 | 87.49 | 8.26 | 3.55 | 0.13 | 0.54 | 2100 | 87.89 | 8.25 | 3.17 | 0.19 | 0.49 | 2500 |
| | 87.65 | 8.26 | 3.35 | 0.17 | 0.54 | 2050 | 88.10 | 7.84 | 3.35 | 0.21 | 0.49 | |
| | 88.24 | 7.80 | 3.18 | 0.19 | 0.56 | 1950 | 87.81 | 8.23 | . 3.29 | 0.17 | 0.49 | |
| | 87.60 | 7.92 | 3.83 | 0.20 | 0.44 | 2300 | 87.60 | 8.14 | 3.57 | 0.22 | 0.45 | |
| | 87.60 | 7.92 | 3.83 | 0.20 | 0.44 | 2200 | 87.60 | 8.14 | 3.57 | 0.22 | 0.45 | |
| 2400 | 87.37 | 8.05 | 3.90 | 0.20 | 0.46 | 2100 | 87.05 | 8.32 | 3.98 | 0.16 | 0.47 | 2400 |
| | 87.37 | 8.05 | 3.90 | 0.20 | 0.46 | 2050 | 87.05 | 8.32 | 3.98 | 0.16 | 0.47 | |
| | 87.35 | 8.02 | 4.00 | 0.18 | 0.43 | 1950 | 86.90 | 8.47 | 3.96 | 0.20 | 0.45 | |
| | 87.45 | 7.93 | 3.96 | 0.18 | 0.45 | 2200 | 87.47 | 8.11 | 3.75 | 0.21 | 0.45 | |
| 2300 | 87.45 | 7.93 | 3.96 | 0.18 | 0.45 | 2100 | 87.47 | 8.11 | 3.75 | 0.21 | 0.45 | 2300 |
| | 87.41 | 8.17 | 3.74 | 0.20 | 0.45 | 2050 | 87.31 | 8.07 | 3.91 | 0.19 | 0.45 | |
| | 87.41 | 8.17 | 3.74 | 0.20 | 0.45 | 1950 | 87.34 | 8.07 | 3.91 | 0.19 | 0.45 | |
| | 87.34 | 8.22 | 3.70 | 0.27 | 0.45 | 2100 | 87.40 | 8.06 | 3.84 | 0.23 | 0.45 | |
| 2200 | 87.34 | 8.22 | 3.70 | 0.27 | 0.45 | 2050 | 87.30 | 8.26 | 3.76 | 0.22 | 0.45 | 2200 |
| | 87.26 | 8.28 | 3.76 | 0.21 | 0.46 | 1950 | 87.26 | 8.17 | 3.89 | 0.20 | 0.46 | |
| 2100 | 87.21 | 8.16 | 3.95 | 0.18 | 0.47 | 2050 | 87.22 | 8.26 | 3.85 | 0.18 | 0.47 | 2100 |
| | 87.21 | 8.16 | 3.95 | 0.18 | 0.47 | 1950 | 87.22 | 8.26 | 3.85 | 0.18 | 0.47 | |

sharply. Two points were below specification requirements for elongation, the 2350 F casting temperature for the 2400 F superheat and 1850 casting temperature for the 2200 F superheat. Note the relatively small degree of scatter between superheat temperatures.

The average tensile and elongation curves shown in Fig. 5 were obtained from two test bars poured from gun metal melted in the open-flame, oil-fired furnace with no phosphor-copper deoxidation. Only three superheat furnace temperatures were run, 2400, 2200 and 2100 F. The curves for both ultimate strength and elongation follow the same trend noted for oil-fired heats deoxidized with phosphor-copper, i.e. the ultimate strengths increased with decreasing pouring temperatures, reaching its peak value at a casting tem-

perature of 2000 F. Below the 2000 F casting temperature, the ultimate strengths decreased sharply. It will be noted that two tensile values, and four elongation values at the lower casting temperature, are below specification requirements.

The elongation values were generally slightly lower in the nondeoxidized heats than in the deoxidized heats at 2050 F casting temperature peak. The ultimate strength values in both cases were on a par at the peak value. At the lower casting temperatures the nondeoxidized heats showed generally lower elongation values. The best combination of tensile and elongation values, with or without phosphor-copper deoxidation, occurs at 2050 F in the open-flame, oil-fired furnace. This is based, of course, on the test bar prac-

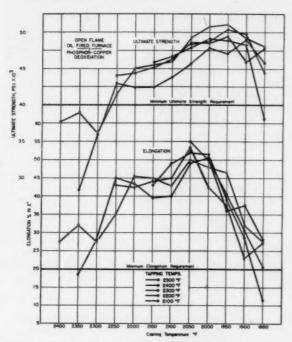


Fig. 4-Effect of tapping and pouring temperatures on mechanical properties of gun metal.

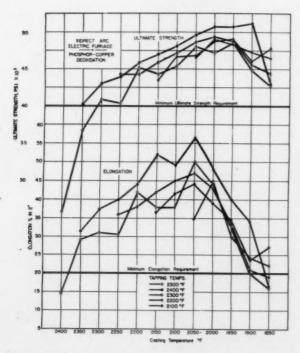


Fig. 6-Effect of tapping and pouring temperatures on mechanical properties of gun metal.

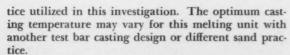


Figure 6 illustrates graphically the average tensile

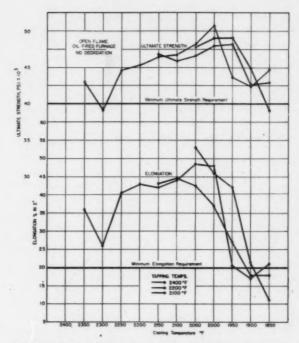


Fig. 5-Effect of tapping and pouring temperatures on mechanical properties of gun metal.

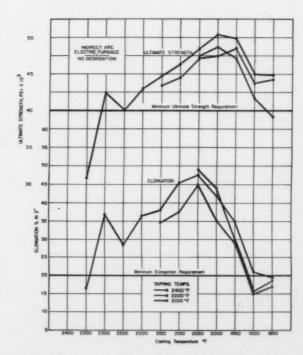


Fig. 7-Effect of tapping and pouring temperatures on mechanical properties of gun metal.

and elongation values obtained from gun metal heats superheated to 2500, 2400, 2300, 2200 and 2100 F in an indirect-arc furnace and deoxidized with 2 oz P-Cu per 100 lb of molten metal. The same trend is noted here in the ultimate strength curves as in both the deoxidized and nondeoxidized open-flame, oil-fired heats, i.e. with decreasing casting temperatures there is an increase in ultimate strength, reaching a peak at a casting temperature of 2000 F and then decreasing gradually. At the 1850 F casting temperature, the ultimate strength results are erratic. The elongation curves follow the same pattern, previously noted in the open-flame furnace, increasing with decreasing pouring temperature to a peak value and then decreasing sharply. The peak elongation values for this series of heats can be considered to be at a casting temperature of about 2050 F.

Tensile and elongation values for nondeoxidized gun metal heats superheated to 2400, 2200 and 2100 F in the indirect-arc furnace are shown in Fig. 7. A uniform trend is noted here, the tensile curve reaching its peak at 2000 F and the elongation curve at 2050 F.

In the indirect-arc furnace heats (deoxidized and nondeoxidized), the best combination of tensile and elongation values occurred at 2050 F, as in both oilfired furnace conditions. One significant point is apparent, however, in the indirect-arc curves-the values obtained from the nondeoxidized heats are slightly higher than the values from the phosphor-copper deoxidized heats which is the reverse of the results obtained in the oil-fired furnaces. It would appear that the phosphor-copper treatment which is normal for crucible practice, i.e. 2 oz per 100 lb is excessive for this unit as it normally runs a predominantly high carbon-monoxide atmosphere that gradually reduces the oxide content of the bath. The phosphor-copper additions in this melting unit, under actual production conditions, should be kept to a maximum of 1 oz per 100 lb of molten metal.

No attempt was made to replenish zinc melting losses due to the variable effect on melt quality and as a result the oil-fired heats tapped at 2300, 2400 and 2500 F resulted in a zinc residual below specification requirements. The zinc losses in the indirect arc furnace were low and the zinc residuals were well above specification requirements. The peak elongation values obtained in the oil-fired furnace were generally slightly higher than in the indirect-arc furnace. The differences in zinc contents could account for this differential.

Effect of Melting Conditions

It is also significant, insofar as tensile and elongation values are concerned that when proper melting atmospheres are employed, the degree of superheat or tapping temperature is not critical. The casting temperature, however, is critical and exerts a profound influence on the mechanical properties and cold fractures. In some instances on other types of quality control specimens studied by the authors, a variation in pouring temperature alone was sufficient to cause localized discolorations in the cold fracture. Too frequently, indications of this character are diagnosed as due to "poor" metal or improper melting conditions.

It is obvious from the results obtained for the particular test bar design utilized in this investigation that the optimum temperature occurs just prior to the transformation from the columnar and equiaxed structure to a completely equiaxed grain structure. This optimum temperature is probably associated with a critical rate or crystallization. Evaluation of the data shown here and data presented previously by Eastwood and Kura would seem to indicate that some of our specifications and concepts involving test bars are in need of revision. It is generally agreed that test bars reflect metal quality and tell us nothing about the condition of the casting. Actually, then, when we speak of metal quality we are concerned with the total gas content of the heat and a medium that is sensitive to changes in this gas content.

We should not be particularly interested in achieving maximum physical properties which are meaningless but in keeping all other variables constant which have an effect on our ultimate evaluation mechanism, in this case, ultimate strength and elongation. Hence it would appear that in a specification where minimum ultimate strength and elongation are specified one must also specify type of test bar design, type of sand, pouring temperature, and rate of pour. For example, different test bar designs will yield different levels of physical properties hence this should be specified.

The type of sand employed and its moisture content will have an effect on solidification rates hence it should be specified. The same applies to rate of pour and it should be controlled. Pouring temperatures will have a profound effect on ultimate and elongation as shown and low properties can be achieved from structural variations which have no direct correlation necessarily with total gas content.

Obviously pouring at a high temperature will reflect total gas content more accurately than a ladle of metal cooled and poured at its optimum temperature. Of course the original choice of test bar design should be such that it will yield reproducible properties and be sensitive to changes to these conditions. Unless these conditions are specified, no specification or no foundryman responsible for metal quality can discriminate effectively between a poor quality heat poured at the optimum temperature and a good quality heat poured at a temperature other than the optimum.

Effect on Macrostructure

The upper part of Fig. 8 shows the macrostructure of longitudinal etched gage sections of test bars which were melted in an open-flame, oil-fired furnace, superheated to 2500, 2400, 2300, 2200 and 2100 F respectively in separate heats, deoxidized with phosphorcopper and cast at 50 F decrements as the metal cooled. The bottom half shows the macrostructure of nondeoxidized bars melted in the same furnace, superheated to 2400, 2200 and 2100 F, and cast at the same 50 F decrements.

The macrostructure in this case generally manifests itself as an outside area of columnar crystallization and a center area of equiaxed grains. With a decreasing casting temperature and an increasing rate of crystallization the columnar crystallization zone is transformed to a random grain orientation, and a completely equiaxed structure prevails.

In the deoxidized heats, the sharp delineation of the equiaxed center zone has begun to disappear, due to RES

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re ch er-ec-or-cal of er-ne

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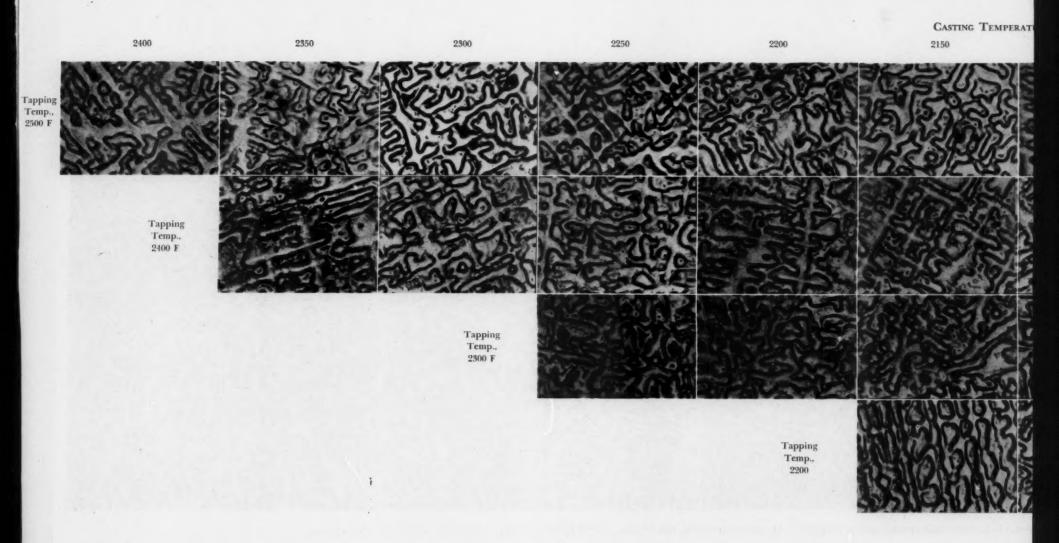
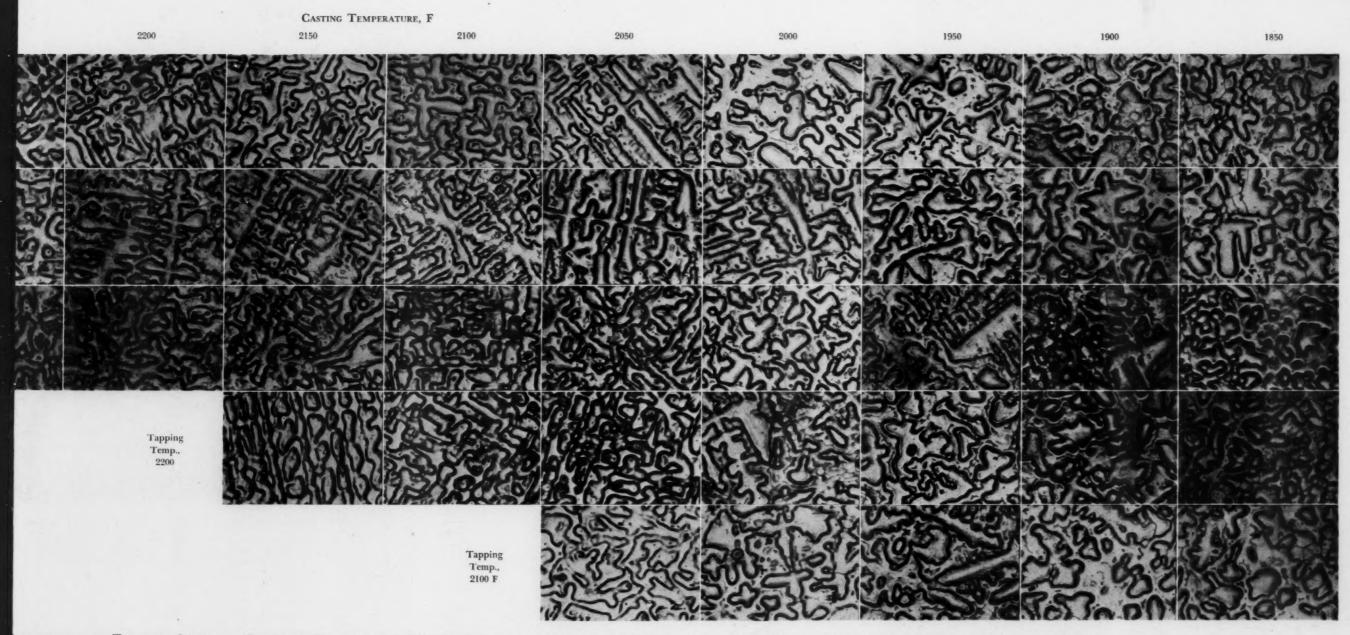


Fig. 14-Effect of Tapping and Casting Temperatures on the Microstructure of Transverse Sections of Gun Metal Test Bars. 2 of



ICROSTRUCTURE OF TRANSVERSE SECTIONS OF GUN METAL TEST BARS. 2 0Z/100 LB PHOSPHOR-COPPER ADDED TO LADLE. MAGNIFICATION: 100 X. ETCHANT: NH4OH+H2O2. FURNACE: OIL-FIRED, OPEN-FLAME

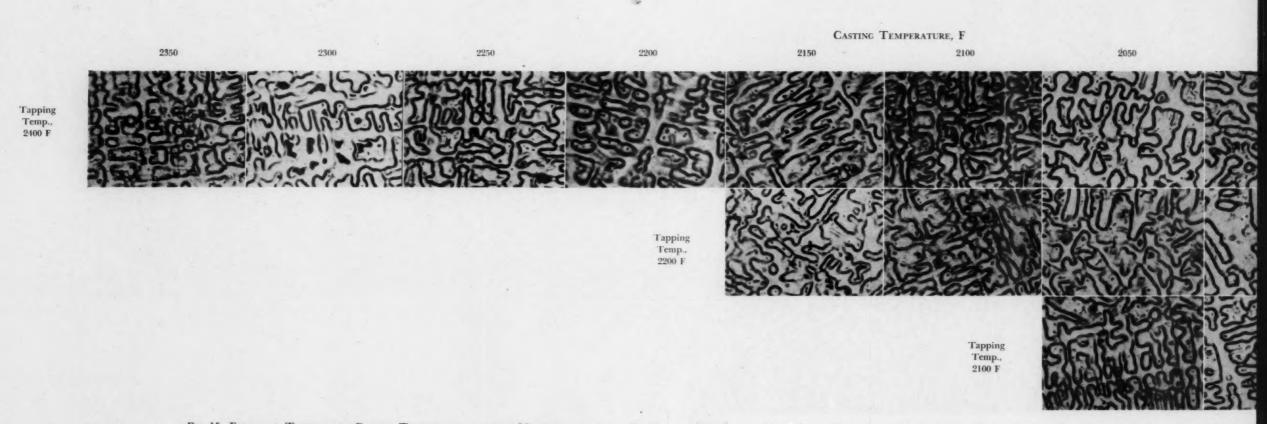
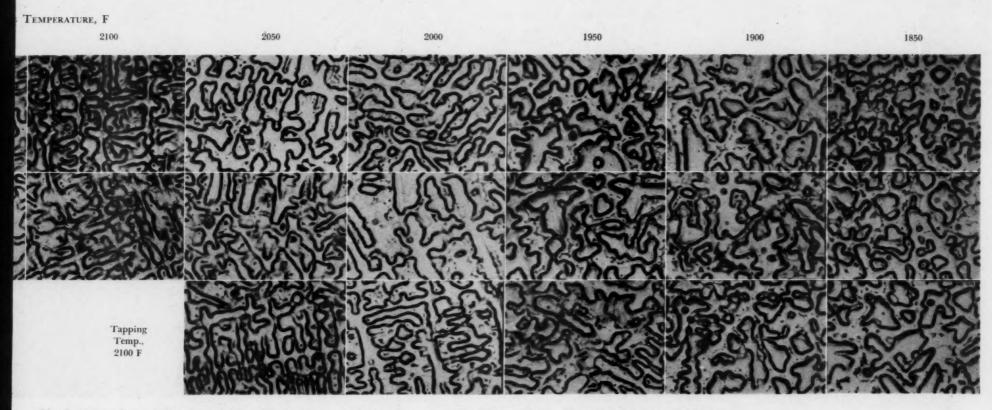


FIG. 15-EFFECT OF TAPPING AND CASTING TEMPERATURES ON THE MICROSTRUCTURE OF TRANSVERSE SECTIONS OF GUN METAL TEST BARS. NO PHOSPHOR-COPPER ADDED TO LADLE. MAGNIFICATION: X 100.



ARS. NO PHOSPHOR-COPPER ADDED TO LADLE. MAGNIFICATION: X 100. ETCHANT: NH4OH + H2O2. FURNACE: OIL-FIRED, OPEN-FLAME.



the columnar zone transformation, at the peak of the tensile strength curves (2000 F casting temperature) and has completely disappeared at the 1950 F casting temperature where a uniform equiaxed grain structure is in evidence. Porosity is prevalent at the two lowest pouring temperatures.

In the nondeoxidized test bars, the "center-line" condition is not as pronounced and begins to lose its identity as in the deoxidized heats at the peak of the ultimate strength curves (2000 F casting temperature). At the 1950 F casting temperature the grain structure

is completely random.

Figure 9 shows the macrostructure of indirect-arc electric furnace test bars. The same general characteristics noted for the oil-fired heats are also apparent here. In both the deoxidized and nondeoxidized heats, the "center-line" condition is eliminated at the 1950 F casting temperature, and a completely equiaxed structure prevails. The "center-line" condition begins to lose its identity at 2000 F casting temperature for both deoxidation conditions. Again, the superheat temperature is not critical with respect to grain size.

It is apparent from the results of the macro examination and from the mechanical property curves that a fine grained equiaxed structure in the tin bronzes is not synonymous with the ultimate in tensile strength, elongation and soundness when this fine grained condition is precipitated by a low pouring temperature. In addition, maximum mechanical properties were obtained just prior to the transformation to a com-

pletely equiaxed structure.

There does not appear to be any grain coarsening as a result of the superheating temperatures. In all instances, both in the deoxidized and nondeoxidized heats in the two melting units employed, the superheat temperature had no significant effect on grain size as evidenced by the macrostructure at any one casting temperature.

Effect on Microstructure

The microstructures of transverse gauge sections, made from the same half of the test bars used for macro examination, for deoxidized and nondeoxidized oil-fired, open-flame and indirect-arc furnace heats are shown in Fig. 10 through 13 and Fig. 14 through 17, respectively. Figures 10 through 13 shows these specimens at X25 magnification, while Fig. 14 through 17 shows the same specimens at X100 magnification. At the higher casting temperatures, in both the electric arc and oil-fired heats a greater amount of gross gas porosity is in evidence than in the lower casting temperatures. At the lower pouring temperatures a greater amount of microshrinkage is in evidence and is always associated with the delta constituent. Above the 2100 F casting temperature the delta constituent is sparse and small. At 2000 F an increase in this constituent is noted. This constituent increases in amount with decreasing casting temperatures and in some instances forms a network at the lower pouring temperatures. This condition in itself may be associated with the decrease in elongation noted from 2050 F down to the lowest pouring temperature. At the 1950 F casting temperature the regular geometric dendritic pattern takes on a completely random configuration with

much smaller dendrites, which is identical with the macro transformation from columnar and equiaxed crystallization to a completely equiaxed fine grain structure. The degree of superheat apparently has no relation to the dendrite size or pattern, or to the microstructure of the tin bronzes, this being influenced primarily by casting temperature or rate of crystallization.

The macro and micro changes, noted in the lower casting temperatures, i.e. the incomplete dendrite formation and equiaxed fine grain structure are probably due to rapid heat dissipation and "nucleation" throughout the cross-section. This increased rate of crystallization will also result in an increase in the eutectoid constituent in the tin bronzes with subsequent lowered ductility. Thus it is apparent from the microstructures that small dendrite size, random orientation of the dendrites and a fine grained structure of the tin bronzes, accomplished through low pouring temperatures, will not yield the ultimate in tensile strength and elongation values.

Microradiography

Microradiographs of selected tapping and casting temperatures in the open-flame, oil-fired furnace and the indirect-arc electric furnace are shown in Fig. 18 and 19. Open-flame heats, both deoxidized and nondeoxidized are shown in Fig. 18 and the same conditions for the indirect-arc electric furnaces are shown in Fig. 19. The nondeoxidized samples from both melting units exhibited a greater degree of unsoundness than the deoxidized samples. Generally, the samples from both melting units, deoxidized and nondeoxidized, exhibited the greatest degree of soundness at the 2050 F casting temperature, regardless of superheat temperature. This could account for the peak tensile and elongation values at this point. Both the high and low pouring temperatures generally exhibit an increase in unsoundness in comparison with the 2050 F specimens. This condition is also reflected in the mechanical property curves. A completely random orientation of the dendrites is in evidence at the 1950 F pouring temperatures which correlates with the data obtained from the microstructures and macros of specimens at the same temperature. The white background noted in many of the 1950 F casting temperature samples is believed to be the delta eutectoid network.

Effect on Hydrostatic Pressure Resistance

Hydrostatic pressure results obtained from the flanged bushings illustrated in Fig. 2, for the ½-in. and ½-in. wall thicknesses, are tabulated in Tables 4 and 5 respectively. The numerals indicate the test pressure at which leakage occurred. Generally, it will be noted that the phosphor-copper deoxidized cylinders from both melting units exhibited greater resistance to leakage than the nondeoxidized cylinders. In the ¼-in. wall thicknesses the cylinders poured at the lower and higher temperatures resulted in the greatest incidence of leakage. Remachining the wall to a ½-in. thickness apparently resulted in too great a sensitivity and increased the effect of other variables. No significant trends based on pouring temperature are apparent at that thickness with the exception of the oil-

OK

OK

TABLE 4—EFFECT OF TAPPING AND POURING TEMPERATURES ON THE PRESSURE TIGHTNESS OF GUN METAL

Test Cylinder Wall Thickness 1/4 in.

Casting Temperature °F **Tapping** -2050-1950 Temp., °F 2300 2200 2100-Open-flame, Oil-fired Furnace, Phosphor-Copper Deoxidation OK 2500 2400 OK 2300 OK OK 2200 OK OK OK OK OK NG NG NG 2100 50 50 50 Open-flame, Oil-fired furnace, No Deoxidation OK NG NG 2500 OK OK OK OK OK OK OK 50 50 OK OK OK OK OK OK OK OK NG NG 2400 OK 2300 NG OK OK OK OK OK OK 50 OK OK OK OK OK NG 2200 50 NG NG NG 2100 OK 400 100 200

| OK | OK | OK | OK | OK | OK | OK | OK | OK | OK | 2400 |
|-----|-----|-----|-----|----------------|--------------|-------------|----------|------|-----|------|
| | | OK | OK | OK | OK | OK | OK | OK | OK | 2300 |
| | | | | OK | OK | OK | OK | OK | OK | 2200 |
| | | | | | | OK | OK | OK | OK | 2100 |
| | | * | Ind | lirect-arc Ele | ectric Furna | ce, No Deox | xidation | | | |
| NG | NG | NG | NG | ОК | ОК | OK | OK | OK | NG | 2400 |
| 150 | 150 | 150 | 100 | | | | | | 100 | |
| OK | OK | OK | OK | OK | OK | OK | OK | NG | NG | 2400 |
| | | | | | | | | 50 - | 50 | |
| | | OK | OK | OK | OK | OK | OK | OK | OK | 2300 |
| | | | | OK | OK | OK | OK | OK | OK | 2200 |
| | | | | | | NG | OK | OK | OK | 2100 |

OK

NG

450

200

Indirect-Arc Electric Furnace, Phosphor-Copper

OK

fired, open-flame furnace heats in which phosphorcopper deoxidation was employed. In this series of heats all cylinders poured at 2200 F are sound. In addition, no significant effects due to superheating temperatures are apparent.

OK

OK

Effect on Density

Density determinations were made of the complete cylinders which had been machined to a 1/8-in. wall thickness. These results are illustrated graphically in Fig. 20 through 23 for each furnace and deoxidation condition. It will be noted that the general trend is for increased density with decreasing pouring temperature. However, the specific gravity trends shown in Fig. 23 for the test cylinders melted in the indirectarc furnace and poured without phosphor-copper deoxidation indicated a wider scatter than is shown for the oil-fired, open-flame furnace, both deoxidized and nondeoxidized and for the castings melted in the indirect-arc furnace and deoxidized with phosphorcopper. Generally, however, no significant effects on specific gravity can be attributed to the superheat variable.

Further observation of the data would seem to indicate that an anomaly exists inasmuch as the highest densities shown are associated with the lowest pouring temperatures and it is in this region where the greatest tendency for leakage and low ductility seemed to occur. In addition, the increasing density trends with decreased pouring temperatures are also associated with increased delta eutectoid formation and increasing rates of crystallization.

Deoxidation

NG

100

OK

OK

2500

It is believed that the latter two phenomena can be utilized to explain the apparent anomaly present in the density data due to the changes in the character of the porosity they cause at the high and low temperature ranges. This will be discussed more fully in later paragraphs. For example, it is considered that the gross gas porosity resulting from a relatively slow rate of solidification and a high pouring temperature, or as some investigators have pointed out gas absorption from the mold, would have a greater effect on density than the microshrinkage resulting from the mode of solidification of the bronzes poured at a low temperature. This is due primarily to the difference in the volumes of voids in each instance.



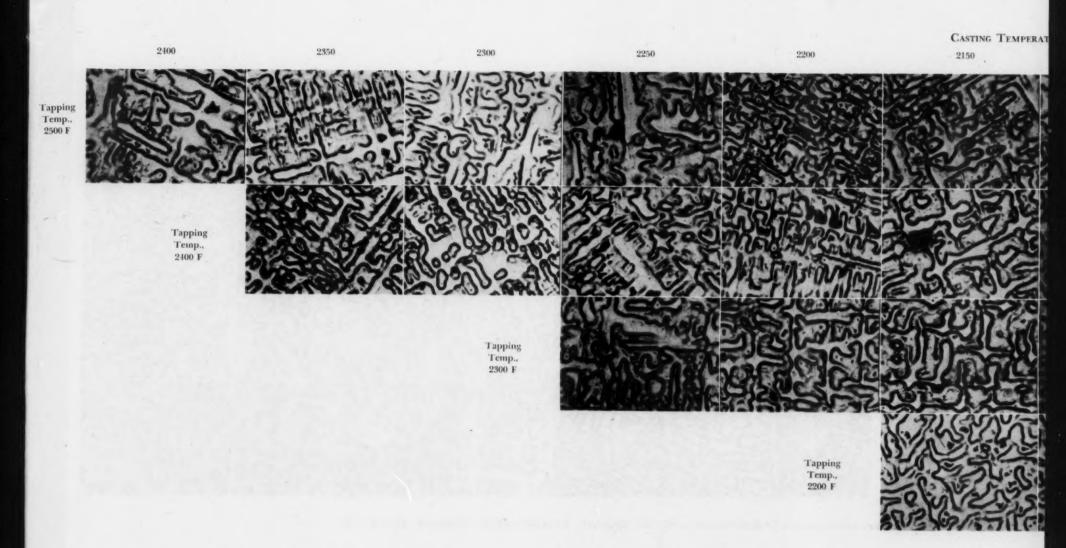
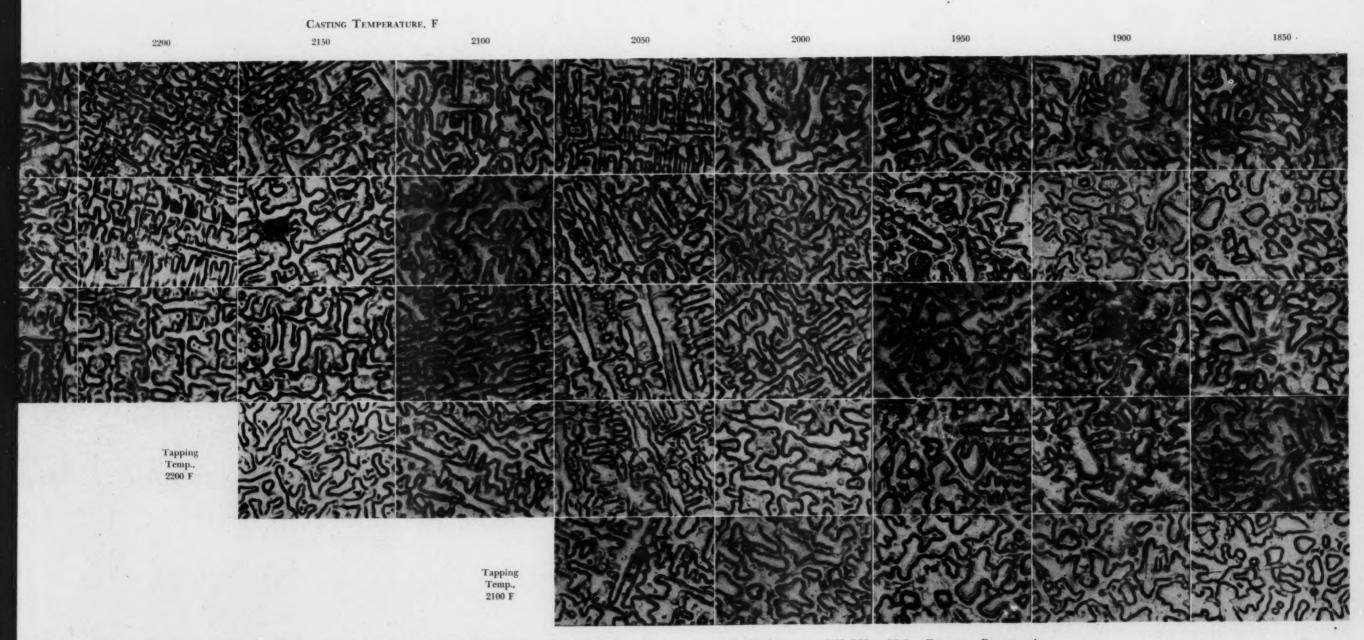


Fig. 16-Effect of Tapping and Casting Temperatures on the Microstructure of Transverse Sections of Gun Metal Test Bars. 2 o



ICROSTRUCTURE OF TRANSVERSE SECTIONS OF GUN METAL TEST BARS. 2 OZ/100 LB PHOSPHOR-COPPER ADDED TO LADLE. MAGNIFICATION: X 100. ETCHANT: NH4OH + H2O2. FURNACE: ELECTRIC ARC.

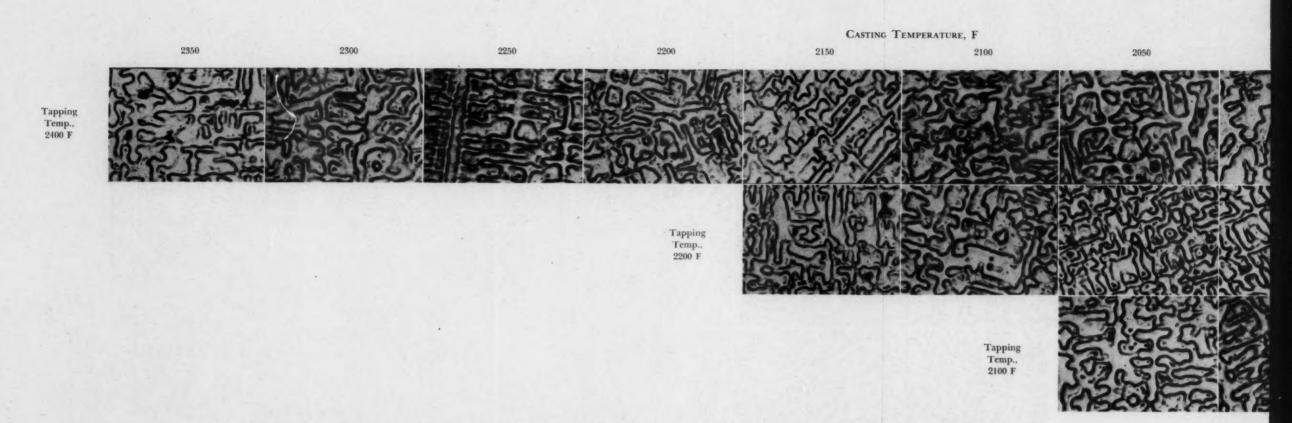
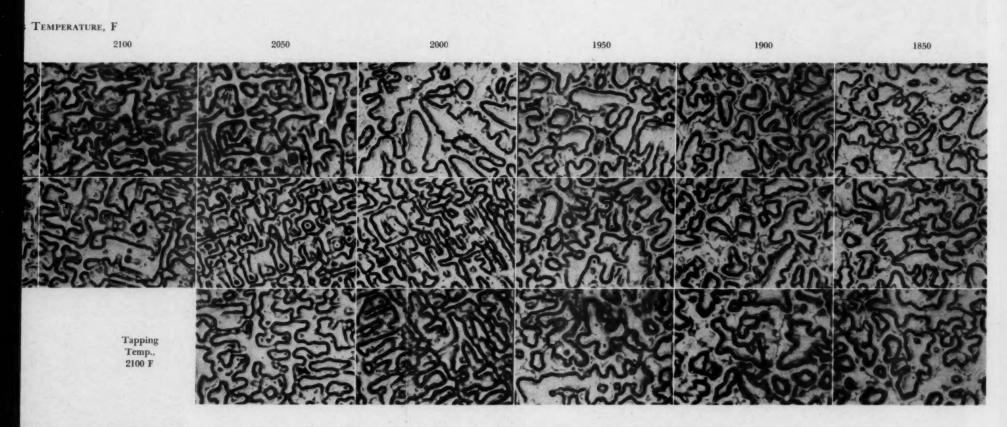


FIG. 17-EFFECT OF TAPPING AND CASTING TEMPERATURES ON THE MICROSTRUCTURE OF TRANSVERSE SECTIONS OF GUN METAL TEST BARS. NO PHOSPHOR-COPPER ADDED TO LADLE. MAGNIFICATION: X



Test Bars. No Phosphor-Copper Added to Ladle, Magnification: X 100. Etchant. NH₄OH + H₂O₂. Furnace: Electric Arc.

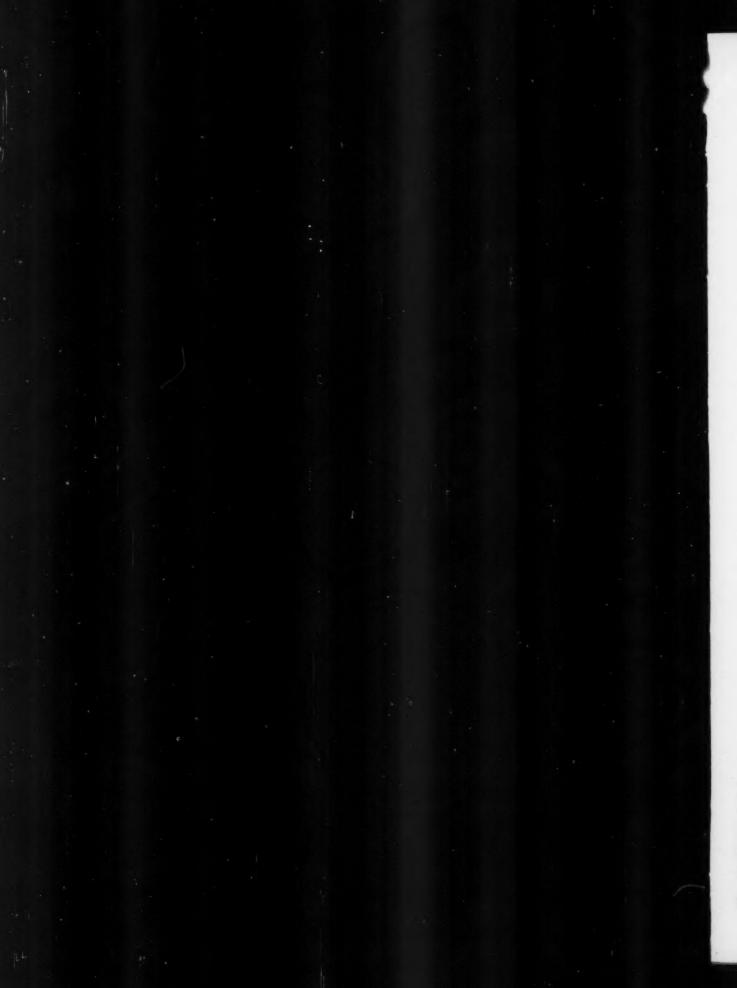


Table 5-Effect of Tapping and Pouring Temperatures on the Pressure Tightness of Gun Metal.

Test Cylinder Wall Thickness 1/4 in.

| 2300 | | | -2200 | | Casting Temperature °F | | | | 1950 | | |
|------|-----|-----|--------------|------------|------------------------|--------------|---------------|-----------|----------|--------------|--|
| | | | Open-flame, | Oil-fired | Furnace, | Phosphor-Co | opper Deoxida | ation | | | |
| OK | NG | OK | ОК | OK | NG | NG | OK | NG | NG | 2500 | |
| ~ | 50 | | | | 200 | 50 | | 50 | 50 | 4000 | |
| NG | NG | OK | OK | NG | OK | OK | OK | NG | OK | 2400 | |
| 50 | 50 | | | 300 | | | | 50 | | | |
| | | OK | OK | NG | OK | NG | NG | NG | OK | 2300 | |
| | | | | 50 | | 200 | 50 | 50 | | 2077 | |
| | | | | OK | NG | OK | NG | NG | NG | 2200 | |
| | | | | - | 50 | | 50 | 100 | 200 | | |
| | | | | | | NG | NG | OK | NG. | 2100 | |
| | | | | | | 50 | 50 | - | 50 | 2100 | |
| | | | | | | | | | - | | |
| | | | Open | -flame, O | il-fired fu | rnace, No D | eoxidation | | | | |
| NG | NG | NG | NG | OK | NG | NG | NG | NG | NG | 2500 | |
| 50 | 350 | 300 | 50 | | 50 | 50 | 200 | 50 | 50 | | |
| NG | NG | NG | NG | NG | NG | OK | NG | NG | NG | 2400 | |
| 50 | 50 | 50 | 50 | 50 | 350 | | 50 | 50 | 50 | | |
| | | NG | OK | NG | OK | OK | OK | NG | NG | 2300 | |
| | | 450 | | 200 | | | | 50 | 50 | | |
| | | | | OK | NG | NG | OK | NG | NG | 2200 | |
| | | | | | 50 | 50 | | 50 | 50 | 4400 | |
| | | | | | | NG | OK | NG | NG | 2100 | |
| | | | | | | 50 | | 50 | 50 | 4.00 | |
| | | | Indirect-Arc | Electric | Furnace. | Phosphor-Co | pper Deoxida | tion | | | |
| NG | OK | ОК | NG | ОК | ОК | ОК | NG | NG | NG | 2500 | |
| 50 | OK | OK | 100 | OK | OK | OK | 50 | 50 | 150 | 2500 | |
| NG | OK | OK | NG | OK | NG | NG | NG | NG | NG | 2400 | |
| 300 | OK | OK | 200 | OK | 100 | 50 | 100 | 50 | 50 | 2400 | |
| 300 | | NG | NG | OK | NG | NG | NG | OK | OK | 2300 | |
| | | 50 | 200 | OK | 50 | 50 | 50 | OK | OK | 2300 | |
| | | 30 | 200 | OK | NG | NG | NG | ОК | OK | 0000 | |
| | | | | OK | 50 | | | OK | OK | 2200 | |
| | | | | | 30 | 50 OK | 100 | NIC | NO | 0100 | |
| | | | | | | OK | OK | NG 200 | NG | 2100 | |
| | | | | | | | | 200 | 50 | | |
| | | | Indir | ect-arc El | ectric Fu | rnace, No De | eoxidation | | | | |
| NG | NG | NG | NG | NG | NG | NG | NG | NG | NG | 2500 | |
| 50 | 50 | 50 | 50 | 50 | 50 | 50 | 50 | 50 | 50 | | |
| | NG | OK | OK | NG | NG | OK | OK | NG | NG | 2400 | |
| OK | 100 | | | 50 | 400 | | | 50 | 50 | | |
| OK | | NG | OK | NG | OK | NG | NG | OK | OK | 2300 | |
| OK | | | | 20 | | 50 | 50 | | | | |
| OK | | 50 | | 50 | | | | | | | |
| OK | | 50 | | NG | NG | NG | NG | NG | NG | 2200 | |
| OK | | 50 | | | NG 50 | | NG 50 | NG 50 | NG 50 | 2200 | |
| OK | | 50 | | NG | | NG | | | | 2200 2100 | |

The changes in the character of discontinuities present in the tin-bronze alloys as a result of pouring temperature are illustrated in Fig. 24 which are positive prints of X-rays of ½-in. longitudinal slices of a wedge casting. Note the change from a relatively sound slice at the 2250 F pouring temperature to the localized gross cavities at the intermediate pouring temperatures and the finely distributed and connected microshrinkage in the slice representing the lowest pouring temperature. If the skin on this casting were to be removed by machining a poor structure and possible leakage under a hydrostatic pressure test may result.

It will be noted that a higher density in itself can

be attributed to the cylinders poured at a low temperature simply from the time differentials noted in Tables 2 and 3 and from the concept that when solid solubility is exceeded, as occurs in the higher tin alloys, or in rapid rates of crystallization and the excess constituent forms a compound (delta), the solid solutions tend to be more dense than calculated from the density of the elements due to a contraction of the alpha lattice. However, the contribution to the density by these causes are not of a high order and not significant compared to the extent of the variations shown in Fig. 20 through 23 which have been attributed largely to differences in volumes of voids caused by changes in the character of the discontinuities.

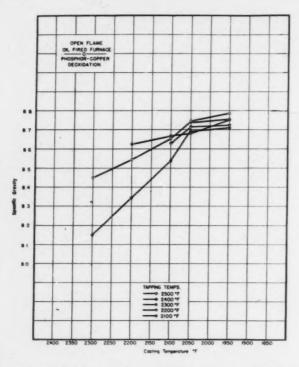


Fig. 20-Effect of tapping and pouring temperatures on specific gravity of gun metal.

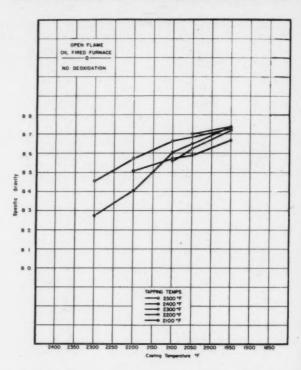


Fig. 21-Effect of tapping and pouring temperatures on specific gravity of gun metal.

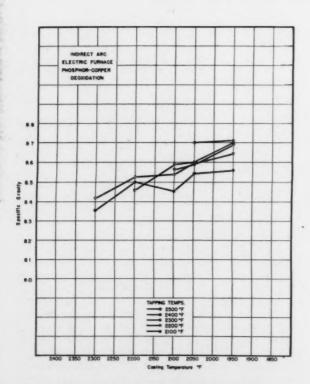


Fig. 22-Effect of tapping and pouring temperatures on specific gravity of gun metal.

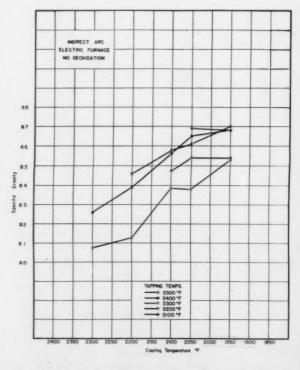


Fig. 23-Effect of tapping and pouring temperatures on specific gravity of gun metal.

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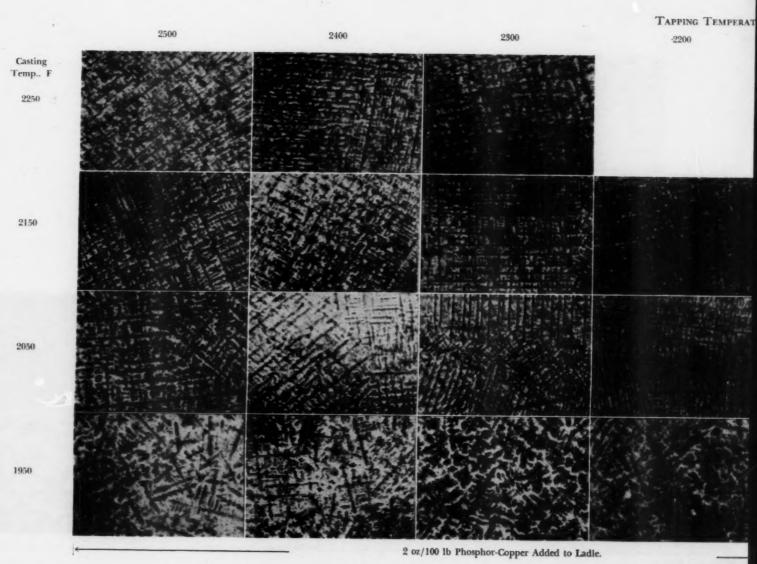


Fig. 18-Microradiographs of Transverse Sec

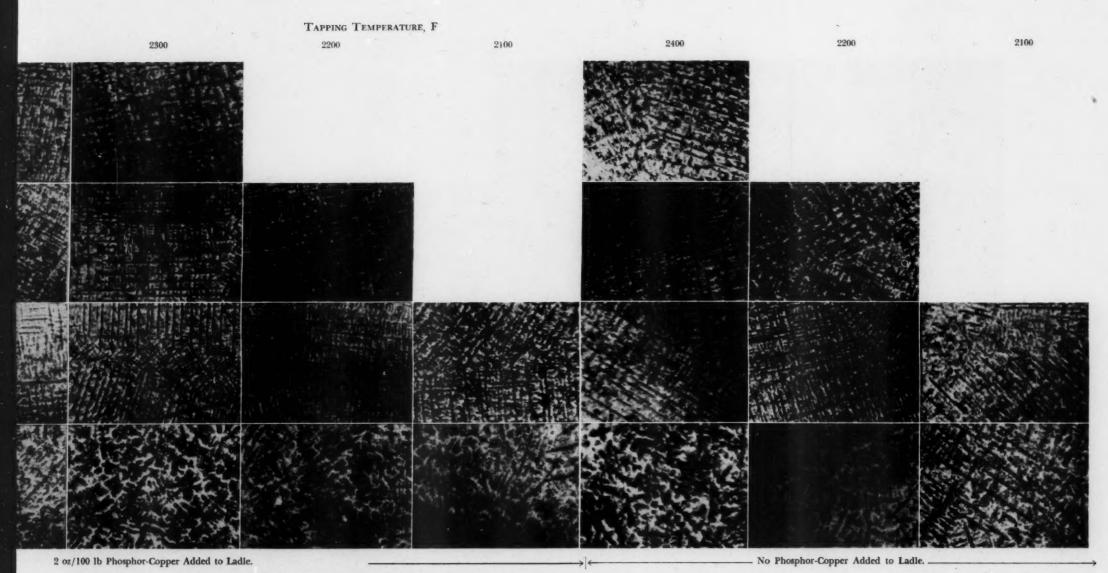


Fig. 18-Microradiographs of Transverse Sections of Gun Metal Test Bars.

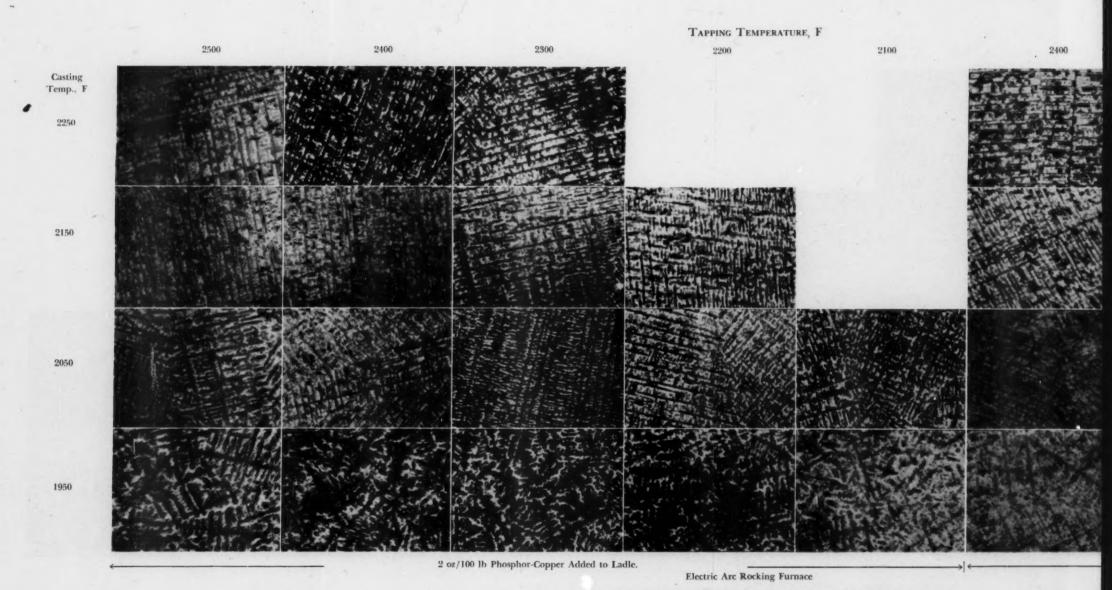
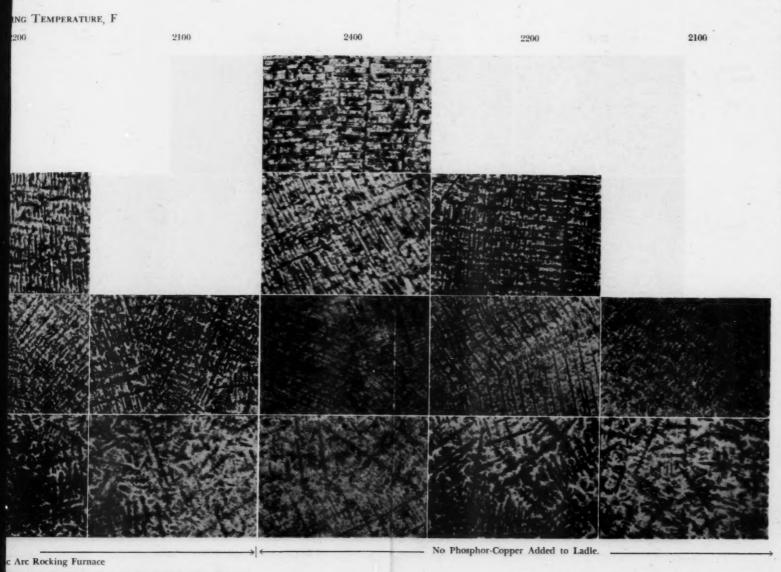


Fig. 19-Microradiographs of Transverse Sections of Gun Metal Test Bars.



RANSVERSE SECTIONS OF GUN METAL TEST BARS.



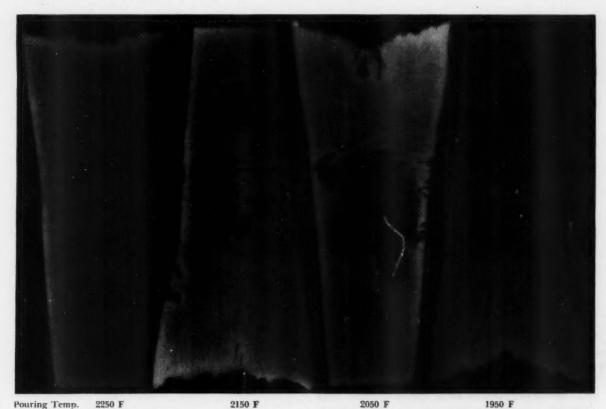


Fig. 24-Positive print of x-ray of 1/8-in. longitudinal slices of wedge casting.

Solidification Mechanism

It is apparent that the ultimate in soundness and mechanical properties in the alloy studied is not associated with a fully equiaxed grain structure. Perhaps the most single important variable by which foundrymen can control grain size is pouring temperature. Superheating temperatures are not considered a significant variable providing the melting atmospheres are not excessively high in hydrogen content.

It is believed that the explanation for the sensitivity of this alloy to pouring temperature is associated with rates of crystallization. An increase in the rate of crystallization will increase the freezing range. Hence low pouring temperatures which precipitate a more rapid rate of crystallization and a longer freezing range will, in effect, increase the "apparent" tin content. Thus during solidification, with relatively low pouring temperatures involved, the amount and tin content of the low melting point liquid metal is increased. This, in itself is not sufficient to explain how a network of microshrinkage forms in this alloy and is almost always associated with the delta constituent.

However, consideration of the mode of solidification between castings poured at low and high pouring temperatures may furnish an explanation. It is believed that castings poured at a low temperature are nucleated throughout the cross-section of the casting, i.e. grain growth is equiaxed, restricted and is initiated at a number of centers. These solidified alpha dendrites envelop pools of low melting point liquid metal, rich in tin, which upon solidification and contraction results in a network of microshrinkage at the dendritic boundaries.

On the other hand relatively high pouring temperatures in the alloy studied are associated with slower rates of crystallization and hence a sparse amount of delta. The tendency under these conditions is for columnar crystallization and the directional type of solidification with octahedral crystals growing normal to the mold wall. Solidification proceeds more closely to equilibrium conditions than in lower pouring temperatures hence the amount and tin content of the low melting point liquid metal is less than in the later. In addition, the pattern of solidification is such that the tendency to occlude pools of low melting point constituent is minimized.

Thus it is probable that the foundryman working with a gun metal bronze may have to design his processing techniques to suit the service conditions of the casting he is manufacturing. For example, higher pouring temperatures may yield the best results from a hydrostatic test pressure point of view yet it is not necessarily associated with the maximum in ultimate strength or ductility. Castings required for bearing services would call for a high rate of crystallization or low pouring temperature so that the delta network can be adequately developed and hence improve the bearing qualities of the alloy.

Summary

On the basis of the results obtained in the course of this investigation the following conclusions can be drawn:

1. When proper melting atmospheres are employed, the degree of superheat or tapping temperature is not critical as reflected by ultimate strength, ductility and

metallographic and micro studies.

2. Casting temperatures, however, have a profound influence on the mechanical properties, macrostructure, and microstructure of cast gun metal. It is obvious that every casting has an optimum casting temperature at which point it develops the highest tensile strength, elongation and greatest degree of soundness.

3. For the design of the test bar utilized in this investigation, optimum properties occurred prior to the macro transformation from columnar and equiaxed crystallization to a completely equiaxed structure and prior to the micro transformation from the regular geometric dendritic pattern to the restricted and random dendrite configuration. It is believed that the optimum mechanical properties of tin bronzes are associated with a critical velocity of crystallization.

4. If all other variables are constant, high pouring temperatures in gun metal bronze will yield a better indication of melt quality than a low pouring temperature. In the latter, structural variations will cause low ductility, and this phenomenon is a function of rate of crystallization rather than potential melt quality. The above presupposes a choice of test bar design which will yield reproducible properties and be sensitive to changes in total gas content at the temperature

specified.

5. The density of gun metal bronze increases with decreasing pouring temperature. Pressure tightness and the ultimate in physical properties however is not necessarily synonymous with high density values. Generally, it may be stated that an equiaxed structure precipitated by low pouring temperatures will not yield the ultimate in tensile, elongation, and pressure tightness. This is due to the difference in the mode of solidification between castings poured at high and low pouring temperatures and the differences in the character of the voids created.

6. Phosphor-copper when utilized judiciously appears to have a beneficial effect on mechanical properties, soundness and pressure tightness. The phosphorcopper deoxidation treatment which is normal for crucible practice (2 oz per 100 lb) is excessive for use in the indirect-arc electric furnace which normally runs a predominantly high carbon-monoxide atmosphere and gradually reduces the oxide content of the bath. In actual production in this unit the phosphor-copper addition should be kept to a maximum of 1 oz per 100 lb.

Acknowledgments

The authors wish to express their appreciation and gratitude to Mr. R. R. Waltien of the Material Laboratory Staff for his assistance in obtaining the data contained in this report. Special thanks are also due to Mr. E. Chrzan and their other associates in the Material Laboratory and Foundry of the N.Y. Naval Shipyard for their assistance. The authors also desire to express their appreciation to Captain D. C. Redgrave, USN, Director of the Material Laboratory, N.Y. Naval Shipyard for his encouragement and review of the paper; to the Navy Department for sponsoring the

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DISCUSSION

Chairman: G. J. BARKER, University of Wisconsin, Madison,

e a p g sl te tl

Co-Chairman: H. J. Roast, Consultant, Westmont, Que., Canada.

G. P. HALLIWELL: 1 This paper corroborates work done by the Battelle Memorial Institute on 85-5-5-5. The shape of the curves with respect to tensile strength and ductility vs pouring temperature are similar in both papers, and serve as an excellent guide to the value of this work.

It has been said and is generally accepted that it is best to pour castings at the lowest possible temperature that will avoid shrinks and misruns. This paper seems to contradict this practice. I would like the authors to reconcile this apparent inconsistency.

There is one other thing I would like to mention. The authors of this paper show that the maximum temperature attained in the superheat is not critical with respect to tensile strength and elongation. It should be realized that their melting conditions were excellent, and that this relationship of superheat to mechanical properties will not hold if poor melting conditions are used.

MR. AMES: From an academic standpoint we would like to reemphasize that superheat temperatures are not significant provided proper melting atmospheres are employed. However, we certainly do not recommend high superheat temperatures in production foundries since they are not only uneconomical but may result in porosity difficulties if melting conditions are not at an optimum.

The results of our investigation do indicate that pouring as low as possible will not yield the ultimate from the standpoint of hydrostatic pressure resistance, particularly on castings in which the benefits of the chilled skin are removed by machining. There appears to be a definite increase in interdendritic shrinkage at the lower pouring temperatures, a condition which is very favorable to "leakers."

MEMBER: The paper states that the optimum pouring temperature is 2050 F. How is one to maintain that temperature of

2050 F when he has to pour 36 sprues?

Mr. Ames: The 2050 F optimum temperature mentioned applies only to the particular test bar design, sand conditions and gating noted previously. However, we do not believe you can pour 36 sprues over a wide range of temperature and obtain consistent results from the standpoint of hydrostatic pressure resistance and physical properties. I believe that is the basic difficulty with many of our production foundries.

MEMBER: That is where many foundrymen are having difficulties because they are trying to do just that.

MR. AMES: That is correct.

MEMBER: This paper brings out the point that that practice must be stopped.

Mr. Ames: The results we obtained, and the work done previously by other investigators, clearly indicates that the tinbronze class of alloys have a narrow pouring range for the opti-

MEMBER: Suppose I decide to strain the metal thereby reducing its velocity. Does that factor enter in?

MR. AMES: The rate of pour will have a decided influence on the solidification characteristics of the alloy and hence will influence the physical properties. We have not investigated that variable. However, Pell-Walpole has studied the effects of pouring rate and attributes considerable influence to this variable on physical properties.

MEMBER: You had a rapid pouring rate. If you used a couple of strainers you would reduce the velocity of metal flow and you would have enough residual heat to get this metal to the place

where you want to get it.

Mr. Ames: Pouring temperatures in themselves are just a means to an end. Actually what we should be trying to control is the rate of crystallization and a change in the rate of pour will affect that variable. Hence a change in the rate of pour should result in a change in the optimum pouring temperature which the foundrymen use as an index.

MEMBER: In other words, we must pour the test bars at one temperature and the casting at another.

Mr. AMES: That is correct.

B. A. MILLER: 2 I want to congratulate the authors on an excellent paper. It would be well to determine the temperature at which the metal comes to rest in the cylinders the authors poured. That is the secret of the whole thing. Use of the heavy gate or light gate has been discussed. There is a slight relationship between pressure tightness and physical characteristics. The test bar is one thing and pressure tightness is another. After all, the average foundry must get pressure tightness and must get physical characteristics in their castings. It would be well to determine the temperature of the metal as it comes to rest in the mold and associate that with the pressure tightness and physical characteristics.

É. D. Boyle: 8 We made several tests pouring gun bronze at

2150 F. We placed thermocouples in the metal as it comes to rest in the mold. These couples indicated a temperature of 1910 F. Whenever the metal temperature was less than 1910 F we would encounter difficulty in pressure-tight castings.

Mr. Ames: Under controlled conditions, there probably is a fairly consistent correlation between pouring temperature and the temperature at which the metal comes to rest in the mold. However, there is no doubt that we would have come much closer to the core of the problem if we had obtained actual melt temperatures when the metal was at rest in the mold cavity.

MR. MILLER: Your gate is quite heavy on most cylinders which is a good indication of what you were striving for.

MR. AMES: We were striving for the optimum condition in feeding the test casting.

FRANK HUDSON: 4 I would like to offer a few remarks on what we in Great Britain think about the relation of dissolved gas in the metal with pouring temperature. In the iron foundry you hear people say, "Pour your castings as hot as possible." In the brass foundry they say, "Pour your castings as cold as possible." Why? I believe that when it comes to cast iron, there is not much gas in it anyhow, so you can do what you like so far as pouring temperature is concerned. With copper-base alloys, such as tin-bronze, if there is a lot of gas in your metal you must handle your metal in the best way and pour it at the lowest temperature permissible. If there is no gas in the metal, then you can do what you like with it as the iron founder does

We feel in Great Britain, and we have done a great deal of work on dissolved gases, that there is a very definite relation between the amount of dissolved gas in the metal and pouring

temperature

The British Non-Ferrous Metals Research Association in Great Britain has done a great deal of work on this very problem.

E. W. MANTLE: 5 The first thing I wish to make clear is that we have found that gas can be picked up in gunmetals and tin bronzes in two ways. First of all, it can be absorbed from the furnace atmosphere, in other words, during the melting of the alloy, and that we call the initial gas content of the metal. Your aim should always be to keep that as low as possible. Secondly, if the alloy contains phosphorus, the phosphorus will react with steam generated in the mold when the casting is poured forming a certain amount of hydrogen, some of which will also dissolve in the metal and give porosity in the casting when it freezes.

We have found that it is not so much the total amount of porosity which is present in a casting that matters, but the way in which the porosity is distributed. It is this factor which governs the pressure tightness or otherwise of a casting, and we have found that gas absorbed by a casting while in the mold

can exert a very noticeable effect on this.

To investigate this in the laboratory, we used a disc casting about 6 in. in diam and 5/8 in. thick with a heavy boss at the center and poured it through a very restricted gate. The casting was thus poorly fed and there was tendency for porosity to be concentrated at the boss. At the same time as these discs were cast, a series of test-bars were also made. The shape of these bars was such that they would be well fed and so reasonably free from shrinkage unsoundness, but because of their section thickness they would freeze rather slowly and so would be sensitive to the gas content of the metal.

The mechanical properties of the test bars were used as a reflection of the gas content of the metal. The disc castings were machined all over and tested under hydraulic pressure. Under normal casting conditions, these discs leaked near the boss.

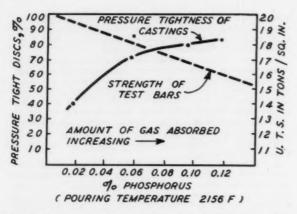
In these tests we used metal initially free from gas, but phosphorus was added to it so that gas would be absorbed while the casting was freezing in the mold.

The trend of the results obtained are shown in the graph.

As the phosphorus content of the alloy increases, the amount of gas the casting absorbs in the mold also increases and this is reflected in the fall in the strength of the test bars. At the same time, however, it was found that the percentage of pressure tight castings made increased considerably. Obviously the gas absorbed by the casting in the mold was bringing about a more favorable

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 Chief Metallurgist, Cramp Brass & Iron Foundries Div., Baldwin Locomotive Works, Philadelphia.
 Master Molder, Puget Sound Naval Shipyard, Bremerton, Wash.

⁴ Development Division, The Mond Nickel Co., London. ⁵ Research, Non-Ferrous Metals Research Association, London.



distribution of porosity around the boss on the casting, and density determinations made on sections cut from these discs confirmed that the porosity was more evenly dispersed and not concentrated in the boss. We have also found that gas absorbed by the metal during melting has a similar but less pronounced influence on the distribution of porosity.

I think these results indicate, as has been shown by Mr. Ames, that maximum strength in the test bar is no criterion of the pressure tightness of the casting. They do show, however, that gas in the metal, particularly gas absorbed in the mold, has an important influence on pressure tightness, in that it can bring about a redistribution of porosity.

Mr. Ames: I am discussing 88-8-4.

Mr. Mantle: I am discussing 80-10-5-5 gunmetal.

L. W. EASTWOOD: 6 I would like to make a few comments in an attempt to tie together some of the information which our British colleagues have given, and the question which Mr. Halliwell raised. Leakers are the result of two causes: One, microporosity, which the original authors referred to as "microshrinkage," and which we prefer to call "microporosity" because it is a result of gas evolution during solidification or inadequate feeding, or both. The other cause of leakage is localized shrinkage, which is, of course, a direct result of inadequate feeding. Microporosity becomes increasingly severe with increasing pouring temperature because gas absorption from the mold increases progressively with increasing pouring temperature. Localized shrinkage usually becomes more pronounced with decreasing pouring temperature, probably because the temperature gradients are less favorable and because unsoundness from gas evolution becomes less and compensates to a lesser extent the contraction of the metal when it solidifies.

We have, then, two principal causes of leakers, one of which gets worse with increasing temperature, and the other gets worse with decreasing temperature. The lowest percentage of leakers will usually be obtained when there is just enough gas evolution to form microporosity to eliminate the localized shrinkage. The reason for this is that localized shrinkage invariably extends across the entire section, particularly of machined sections, whereas microporosity does not necessarily do so. It is better, therefore, to have some microporosity than to have localized shrinkage. The amount of gas evolution during solidification should be just sufficient to eliminate or adequately reduce localized shrinkage.

It is apparent, therefore, that a perfectly fed casting will not require gas evolution to eliminate localized shrinkage. A poorly fed casting will require a substantial amount of gas evolution to eliminate the localized shrinkage. It is further evident, then, that the amount of gas evolved during solidification to reduce shrinkage and produce greater percentage of leak tightness will depend upon the design of each individual casting, the method of gating it, and other foundry variables.

Accordingly, the relationship between the per cent of leakers vs the per cent of phosphorus, illustrated by Mr. Mantle, is also specific for each individual casting design. A perfectly fed casting would not require any phosphorus to prevent leakers, whereas a poorly fed casting will require more phosphorus to reduce leakers than a fairly well fed casting.

To sum up then, both high phosphorus and high pouring temperatures tend to increase the amount of gas absorbed from the mold and increase the amount of microporosity. The pouring temperature or phosphorus content required to produce enough microporosity to just eliminate the localized shrinkage and produce a maximum percentage of leak-tight casting will then be specific for each casting design.

G. E. DALBEY: ⁷ I would like to ask Mr. Ames if he found any material difference in residual phosphorus in the metal melted in the indirect-arc furnaces as compared to the metal melted in the open-flame furnaces that might account for his differences in results?

MR. AMES: The differential in phosphorus shown would be about 0.01 per cent and not significant insofar as the results of mechanical properties obtained in the open-flame furnace and indirect-arc furnace are concerned. The difference in physical properties can be attributed to the differences in zinc residuals due to the excessive loss of zinc in the open-flame oil-fired furnace.

I would like to make a few remarks in regard to the discussion of Mr. Mantle. We have not ignored the effect of gas porosity on the mechanical properties of the alloy studied. In fact we do mention that it is a factor. However, we do believe that it is an overemphasized factor and not nearly as important a variable as the solidification characteristics of the alloy concerned. For example Fig. 24 illustrates exographs of ½-in. longitudinal slices of a wedge casting cast in green sand. This is a very sensitive inspection technique for disclosing defects such as gas porosity and interdendritic shrinkage. Note the relatively sound slice poured at 2250 F, which would be most sensitive to gas pickup from the mold as compared with the interdendritic shrinkage cavities contained in the wedge poured at 1950 F. This latter defect is not a result of gas porosity but ather a result of the solidification characteristics of the alloy. It is this connected channel type defect which is more apt to result in "leakers" than the shape of the defect which results from gas

MEMBER: I heard much comment on pickup of gases due to higher temperatures compared with the so-called optimum temperature. Does not the gas evolve from the molten metal as the temperature drops? Will not the metal pick up gas and is it not going to evolve a certain amount of it before you pour the metal?

MR. AMES: That is true.

MEMBER: Is it not a fact where time is not an element you can pour the metal by overheating it and then reducing the temperature?

MR. HALLIWELL: I would like to call attention to the fact that some of these differences in properties may be due to compositions that run all the way from 0.62 Zn to nearly 4 Zn. One alloy will have a copper of 90.5 and the other 87 per cent. That will make a difference in properties.

Mr. Ames: We were not particularly concerned with actual quantitative data. What we wanted to illustrate was trends and in the five superheats for each furnace you will notice that the degree of scatter is relatively small. We were aware of the wide differences in chemistry due to the melting conditions that were imposed on the various heats and realized that they would have some influence on the quantitative level of physical properties.

B. M. Loring: For some time we have been interested in the changes in microstructure with pouring temperature. It is only natural to expect a more favorable combination of properties in alloys of fine grain size. Yet the authors show not only no improvement in mechanical properties but a decided decrease when the grain size has been refined by low pouring temperature to 1/100 or 1/1000 of its former size at high pouring temperature. The authors pointed out that many other factors besides grain size contribute to the mechanical properties.

With the intention of avoiding some of these factors we have refined bronze poured at 2200 F to about 1/100 or more of its usual grain size by vibration techniques. We find possibly a 10 per cent increase in mechanical properties under certain conditions, but nothing to justify the difficulty in use of the apparatus. Quite often, before we learned the technique of refinement by these different methods, we obtained lower mechanical properties than would be found in coarse grained bronze. We should pay more attention perhaps to the statement made by the authors, that "low properties can be achieved from structural variations which have no direct correlation necessarily with total gas content."

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INFLUENCE OF TEMPERATURE ON FLUIDITY AND SURFACE APPEARANCE OF STEEL CASTINGS

By G. A. Lillieqvist*

ABSTRACT

Three types of temperature equipment used in the investigation are briefly discussed.

Information obtained shows the effect of various melting units on fluidity of steel at comparable temperatures.

Various factors such as ladle preheat temperature, tapping temperatures and correlation of pouring temperatures and casting defects are discussed with relation to bath metal temperature control.

Introduction

Through the work of other investigators and our own experiences, it has become apparent that accurate control of liquid steel temperatures is of prime importance in the production of quality steel castings. In these changing times industry has become more and more quality minded and through competition, the future points to even higher quality standards. Engineering changes and new product design have emphasized the importance of temperature control. There is no doubt that temperature control is absolutely necessary in production of quality castings, and with it the ultimate cost of producing these quality castings will be reduced.

Some of the information presented in this paper merely represents trends of various experiments. It is hoped that this paper will stimulate thought and encourage further work on this subject.

Equipment Required for Temperature Measurement

In order to make a thorough study of the effect of temperature and fluidity on the surface appearance of steel castings it is essential to have the necessary equipment which will give accurate and reproducible results.

Until a few years ago, the furnace operators have been the sole judge of estimating molten steel temperatures in the furnace. This, as expected, resulted in considerable variation in tapping temperatures. In reality the estimation of temperature is merely an individual's opinion which may be greatly influenced by any number of factors.

The portable optical pyrometer such as commonly used during the tapping and pouring of a heat is a good instrument under ideal conditions; however, it has definite limitations. While the portable optical pyrometer is a step ahead of the melter's eye, it does not always give the accuracy required for good control because the ideal conditions are seldom present.

In recent years various steel producers working with instrument suppliers have developed satisfactory equipment for the recording of molten steel temperatures. These are the platinum-platinum 10% rhodium immersion thermocouple, the Rayotube and the photoelectronic equipment. These instruments when handled properly produce accurate and reliable results.

At the Research Laboratory of the American Steel Foundries, and at one of our acid electric plants we are equipped to take temperature readings in the melting furnaces by using the platinum thermocouple. Figure 1 illustrates the equipment used at the Research Lab-

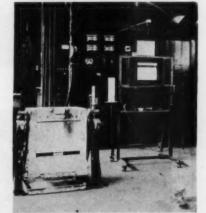


Fig. 1

oratory. Similar equipment, only on a larger and more elaborate scale, is used at an acid electric plant. See Fig. 2. Space limitations do not permit detailed discussion of this equipment, but satisfactory information can be obtained from the supplier.

At one of our basic open hearth plants the Rayotube equipment was installed sometime ago and now is being used successfully. See Fig. 3.

^{*} Research Director, American Steel Foundries, East Chicago, Ind.

Fig. 2









Fig. 6



Fig. 7



Fig. 3

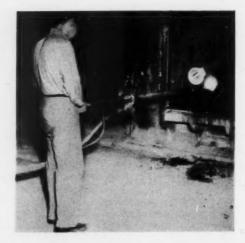


Fig. 5



Fig. 8

Recently photoelectronic equipment for measuring bath temperatures has been installed at two additional open hearth plants of our company. See Fig. 4 and 5. While these installations are fairly recent, considerable information and correlations have already been obtained. For detailed description of these instruments considerable information is available in published papers and in booklets published by the suppliers. In addition recording equipment has been purchased to obtain pouring temperatures as the steel enters the molds.

Fluidity Tests at the Research Laboratory and Plants

To aid in the correlation of metal bath temperatures and various foundry defects and surface appearance it is necessary that accurate pouring temperature data are obtained. This is premised on the fact that there are numerous variables between the furnace and the mold which can cause temperature variations. Along with the pouring temperature studies, fluidity investigations were included.

For many years various investigators have been interested in temperature and fluidity tests. In 1941 Taylor, Rominski, and Briggs¹ published considerable information in the A.F.S. Transactions regarding various types of fluidity tests and the effect of certain elements on fluidity of steel.

After considerable investigation of various types of fluidity tests, it was concluded that the double spiral test was the best for the purpose of the studies at the American Steel Foundries. The rough spiral fluidity casting weighing about 90 lb can be seen in Fig. 6. Consistent results were obtained when this type of test mold was used both with shank pouring and bottom

poured ladles although with the bottom pour ladle the size of the well was increased.

When shanking the steel it was possible to take direct temperature readings in the pouring cup of the fluidity mold by using the setup as shown in Fig. 7 and 8. A fused quartz tube about 6 in. long and sealed on one end was fitted at an angle of 45 deg. into the pouring cup. A 32 B&S gauge platinumplatinum 10% rhodium thermocouple was inserted into this quartz tube which had a 4 mm outside diameter and a 1/2 mm wall thickness. A single hole insulator prevented the two wires from shorting in the quartz tube. The thermocouples were connected to a high speed recording potentiometer which recorded the actual pouring temperature. Pouring temperature readings using this method can be obtained in about 7 to 12 sec. So far excellent comparisons using this type equipment have been obtained in the Research Laboratory and plant studies.

At the Research Laboratory fluidity curves were obtained for various grades of steel such as Grade "B" and intermediate manganese steels with varying temperatures from 2750 F to 2940 F. Thus far indications show that temperature is one of the most important factors influencing fluidity and surface finish of castings. Figure 9 shows the effect of temperature on two grades of steel melted in the induction furnace at the Research Laboratory. The round dot represents Grade "B" steel showing a fluidity of 10 in. at 2750 F and 26 in. at 2930 F. Square dots illustrate the

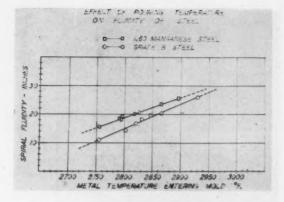


Fig. 9

intermediate manganese fluidity curve which is considerably above the Grade "B" curve especially at the lower temperatures. At 2750 F it had a fluidity test of 15 in. as compared to 10 in. for the Grade "B" steel. In other words, the manganese grade of steel will flow further at the same temperature than Grade "B" steel. This difference has also been noted on certain castings especially on flat plates. At the Laboratory a smoother surface free from wrinkles has been consistently obtained on plate castings when pouring manganese steel than when pouring Grade "B" steel, especially at lower pouring temperatures.

To obtain additional information for Grade "B" and intermediate manganese steel further experiments were made at the Research Laboratory using aluminum and titanium as special deoxidizers. In general, there was very little difference between aluminum and titanium deoxidized steels of like composition at similar pouring temperatures.

In the temperature and fluidity studies at the plants using bottom poured ladles, satisfactory temperature readings in the pouring cup of the fluidity test mold were not obtained because of the faster rate of the metal entering the mold. In order to obtain accurate temperature readings in the foundries using bottom pour ladles, it was necessary to take temperature readings in a mold where a larger amount of steel was poured through the cup containing the thermocouple. It was necessary to have a solid stream of metal in constant contact with the quartz tube for at least 10 sec. Therefore, in plants using bottom pour ladles, the temperature readings were taken in the cup of a fairly large mold poured just prior to the fluidity test mold. In addition, because of the higher static pressure developed in bottom pouring, it was necessary to modify the thermocouple arrangement in the pouring cup so as to eliminate bending and breaking.

A number of different methods were investigated and after considerable experimentation a satisfactory procedure was found. Accurate results were obtained by placing a fused quartz tube across the pouring cup opening near the bottom as shown in Fig. 10. With this arrangement it was possible to use a 4 mm outside diameter quartz tube with a 0.75 mm wall thickness. Because no insulators near the thermocouple bead were required, as one wire extended in from

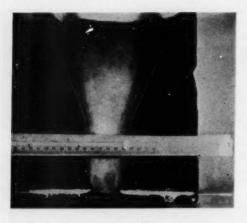


Fig. 10

one side and the other from the other side, the couple responded to the steel temperature faster. This allowed the use of the heavier walled tubing which did not bend in the metal stream.

A number of other precautions had to be followed to obtain reliable temperature readings. First the two junctions of the thermocouple and lead wire had to be well insulated so that during the pouring no rise in temperature at the junctions occurred. Heating of these junctions could cause considerable errors. In the second place, the pouring cup had to be continuously filled with liquid steel during pouring so that the metal was always in contact with the quartz protection tube.

Using this type of testing procedure fluidity curves for Grade "B" steel were established for acid electric and basic open hearth steel. Results were similar to those bound from induction furnace heats at the Research Laboratory. Figure 11 compares the fluidity curves from the three various melting units.

It will be noted that the basic open hearth steel had the lowest and the acid electric furnace steel the highest fluidity. It is believed that this difference is mainly due to the sulphur content of the various steels, the basic open hearth having an average sulphur content of 0.025 per cent and the acid electric furnace 0.040 per cent. The induction furnace heats had an average sulphur content of 0.033 per cent. This effect of sulphur on fluidity has also been observed by English investigators.

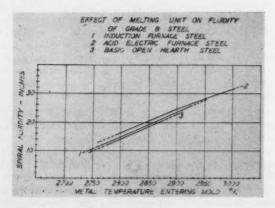


Fig. 11

In the beginning of the fluidity and temperature studies at the basic open hearth and electric furnace plants large variations in tapping and pouring temperature were observed. These variations from heat to heat varied as much as 200 F. As time progressed the operators and melters became more familiar with the equipment and used it to their advantage. Since then the temperature variations have become much smaller. There is no doubt that the temperature measuring equipment will be a vital factor in aiding the operators in controlling the furnace operation and improving their practices.

Results of Temperature Studies in Acid Electric Shop Using 6-Ton Bottom Pour Ladles

Before a definite pouring temperature range can be recommended it is also necessary to consider other factors, such as ladle preheat temperature, type of product, etc. Preliminary studies indicate that variations in preheat time of a 6-ton ladle can vary the temperature loss of molten steel from 200 to 300 F. Figure 12 illustrates this condition. For instance, with

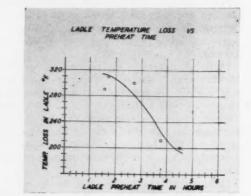


Fig. 12

a ladle preheat time of $1\frac{1}{2}$ hr the temperature loss from tapping to pouring was approximately 300 F, while for a ladle preheat time of 4 hr the loss was approximately 210 F. It is well realized that this method of determining ladle temperatures before tapping is not too accurate. Considerable more work is contemplated in the near future to determine ladle temperatures more accurately and to find the most efficient manner of preheating, both for small and large ladles. Effect of other factors on pouring temperatures such as ladle lining thickness, additions such as ferroalloys and deoxidizers, amount of slag, etc., must be investigated.

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Preliminary work was undertaken to study pouring temperatures and casting surface appearance. A specific production casting weighing about 12 lb was selected and studied in all phases of production including detailed and extensive molding sand tests of the various batches of sand used to make the molds.

The physical properties of the sand mixes varied to a considerable degree and the only definite correlation obtained was between metal temperature and casting surface appearance. While actual temperature readings were not taken in the molds, fluidity tests were poured. The results from the fluidity test were used as a means of determining pouring temperatures. This was possible because of the excellent correlation obtained in previous work between pouring temperature as determined with the platinum-platinum 10% rhodium thermocouple and length of spiral of fluidity

After the castings, from various heats, were shaken out and blasted they were carefully inspected and rated into one of the following categories:

W-Surface wrinkled and letters not sharp

SW-Surface slightly wrinkled

OK-Satisfactory

SR-Surface slightly rough or slight metal penetration

R-Surface rough or metal penetration

VR-Surface very rough or metal penetration

It is possible other investigators might classify their results differently, but this system proved satisfactory in this investigation. Figure 13 illustrates the results

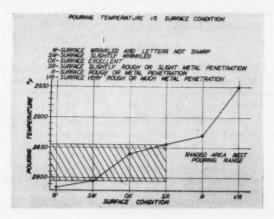


Fig. 13

which were obtained. It will be noted that the correlation between surface condition and pouring temperature is good. With this information it will be possible to establish a desired pouring temperature for this particular type of casting produced in acid electric steel. For other types of castings and melting conditions similar investigations would have to be conducted.

Another interesting observation was made during the pouring of the heats. In practically all heats there was a definite increase in pouring temperature from the beginning to the end of the heat. Fluidity also increased as the temperature increased. It is, therefore, important that in comparisons of heats, temperature measurements of fluidity tests must be taken at a specified time during the pouring operation.

Results of Temperature Studies in Basic Open Hearth Shops Using 25-Ton Bottom Pour Ladles

Considerable investigations have been performed at our three basic open hearth shops. D. Lawther,2 Chief Chemist, of the East St. Louis Works presented an interesting paper at the last National Open Hearth Conference in Chicago on the subject "The Utilization of Open Hearth Bath Temperature Measurements in

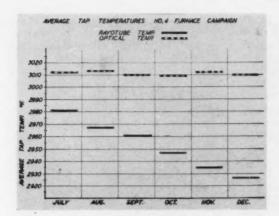


Fig. 14

the Production of Steel Castings." Some of the information presented in this discussion is taken from his

One of the first observations was the noticeable and consistent drop in tapping temperature during a furnace campaign. The average monthly tapping temperature steadily decreased from slightly over 2980 F for the first month of operation to a low of about 2925 F for the last month of the furnace campaign. Along with the Rayotube temperatures, optical temperature readings were taken during tapping but showed practically no variation. Figure 14 illustrates the temperature obtained with the Rayotube and optical pyrometer during the progress of a furnace campaign. Even though furnace times were increased, it was found exceedingly difficult to obtain higher tapping temperature near the end of the furnace campaign.

In this investigation it was also revealed that greater ladle temperature loss is obtained at the higher tapping temperatures. Figure 15 illustrates this condition. It can be noted that at a tapping temperature of 3050 F the temperature loss is about 200 F while at a tapping temperature of 2950 F the loss is only about 150 F for Grade "B" steel. These results were obtained after about one-half of the metal was poured from a 25-ton bottom pour ladle. This is, of course, a trend which may vary with various size ladles and varying

conditions.

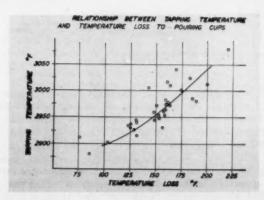


Fig. 15

In checking Rayotube tapping temperatures at one of the basic open hearth plants against per cent defective castings due to cold shuts and misruns, a definite correlation similar to that obtained at the acid electric steel plant was observed. Results can be seen in Fig. 16 where the high tapping temperatures of 2980 F completely eliminated the presence of misruns and cold shuts. This was not just the observation from one furnace campaign, but was consistently observed in other furnace campaigns in this particular plant.

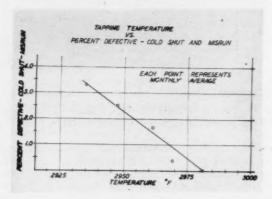


Fig. 16

While the misruns and cold shuts decreased with the increase in tapping temperature, the reverse was true in the comparison of tapping temperature and per cent of defective due to metal penetration and sand inclusions. As shown in Fig. 17 the per cent defective resulting from sand inclusion increases with an increase in temperature.

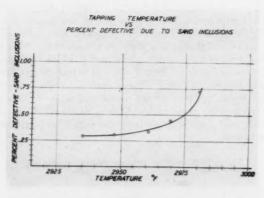


Fig. 17

It was further observed that with proper temperature control the overweight of castings could be materially reduced with a result of a considerable saving of money. There is also a relationship between a definite temperature rise in degrees per minute and oil firing rate. After this correlation has been computed even closer control of metal temperatures can be expected. It has also been noted at one of the open hearth shops that on Grade "B" steel there is a decided increase in pouring temperatures from the mid-

dle of the heat to the end of the heat. This increase takes place near the end of the heat.

There are numerous other factors being studied at the present time, but insufficient information is available for discussion. The American Steel Foundries organization, because of the results obtained thus far have become more and more temperature conscious. The melters and operators who are using temperature control equipment are tapping heats within closer temperature limits than when the equipment was first installed. Other standardizations will have to be made before complete control of pouring temperature can be expected. Each plant will undoubtedly have to set up their own requirements, but we are convinced that the use of temperature control equipment at the furnace and in the foundry in conjunction with fluidity tests will pay dividends in the overall picture where higher quality standards are being required.

While it is possible to draw some conclusions from our results to date, considerable information is on a preliminary basis and is merely presented as a means of stimulating further thought and work.

Acknowledgment

I wish to express my deepest thanks to the personnel of our East St. Louis, Alliance, Granite City and Indiana Harbor Works for their splendid cooperation in this investigation, as well as to C. G. Mickelson and J. A. Rassenfoss of the Research Laboratory in the preparation of this paper.

References

1. H. F. Taylor, E. A. Rominski and C. W. Briggs, "The Fluidity of Ingot Iron and Carbon and Alloy Cast Steel," Transactions, A.F.S., vol. 49, p. 1 (1941).

2. D. Lawther, "The Utilization of Open Hearth Bath Temperature Measurement in Production of Steel Castings," Open Hearth Proceedings, AIME (1949).

DISCUSSION

Chairman: R. C. Wood, Minneapolis Electric Steel Castings Co., Minneapolis.

Co-Chairman: D. C. Zuege, Sivyer Steel Castings Co., Milwaukee.

R. E. Kerr (Written Discussion): 1 In the writer's opinion, two of the biggest variables in modern steel foundry practice are sand mixes and behaviors, and metal pouring temperatures. Much work of a theoretical and practical nature has been performed on the former and here we have a start on the latter. This paper presents data which verifies what we have long suspected but have not proven under closley controlled tests; namely, that many casting defects often attributed to sand variables may actually be caused by metal temperature variables. Very often we have looked high and low for the cause of penetration or a hot tear in a casting, and have dismissed metal temperature because it was the same as we always pour it, but was it?

We produce a material out of the ordinary for most steel foundries, namely, austenitic manganese steel; still, I believe you will be interested in a test we performed. 23

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A core box was made so that nearly identical castings could be obtained, the cores were made from the same batch of sand, were baked together, and were poured from the same heat. The temperature of the metal was taken in the ladle by means of a small portable platinum platinum-rhodium thermocouple just prior to pouring. The temperatures which we so obtained fall short of being as authentic as Mr. Lillieqvist's; however, they did indicate a trend. The castings, after pouring, were allowed

¹ Metallurgist, Pettibone Mulliken Corp., Chicago.

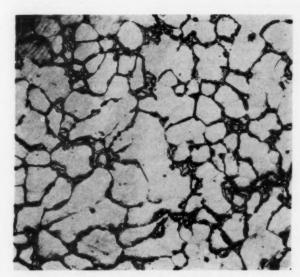


Fig. A-Micrograph showing grain size at 50X. Metal temperature in ladle 2580 F.

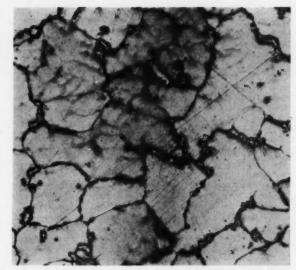


Fig. B.—Micrograph showing grain size at 50X. Metal temperature in ladle 2610 F.

to cool to room temperature in the mold, they were then sectioned in identical locations and checked for grain size.

Figure A, at 50X, shows the grain size as poured from metal with a ladle temperature of approximately 2580 F. Figure B, at 50X, shows the grain size as poured from metal with a ladle temperature of approximately 2610 F. You guess what the metal temperature and cooling rate was on the metal from Fig. C, also at 50X, taken from a casting with a hot tear.

Just a word of assurance here for the carbon and low alloy steel producers, austenitic manganese steel is much more susceptible to excessive grain growth during pouring, solidification, and heat treatment than the steels you are pouring.

The writer realizes that the paper under consideration is an introduction to the work being performed and is intended largely to stimulate thought in the industry; however, the writer believes that incorporation of the following would enhance the paper's value.

 Give the high-low spread of the principal elements in the Grade "B" and intermediate manganese steels of Fig. 9.

2. Discussion of Fig. 11 states that it is believed that the fluidity differences are due to sulphur content; it should be stated whether there was any attempt to correlate other elements.

3. The range of sand properties mentioned in the last paragraph of page 264 should be included in this report.

4. It might be of value to future investigators if the range of plotted points were shown on Fig. 13 rather than an average curve only.

5. It would be interesting to show graphically for one representative heat the graph of metal temperature vs time of hold from beginning to end of pouring operations. The writer would also like to have Mr. Lillieqvist give his thoughts on why the metal temperature raised from the middle of the heat to the end of the heat. How was the metal temperature measured to determine this, and was this phenomenon noted on one or all types of ladles?

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6. Was there any correlation between average monthly scrap and the data presented by Fig. 14? The writer realizes that Fig. 16 and 17 cover metal temperature-casting defects relationships; however, he was curious whether the correlation had been made with Fig. 14.

Mr. LILLIEQVIST: In answer to your question whether there were other elements which could have influenced the fluidity besides sulphur, the answer is no. The chromium, molybdenum, nickel, vanadium, and some of the more common alloys were practically all the same in the basic open-hearth, acid electric and induction furnaces.

There was quite a spread in moisture content, permeability, green compression and dry compression in the molding sand.

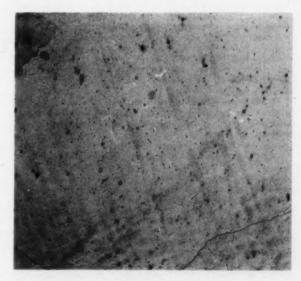


Fig. C-Micrograph at 50X of a casting with a hot tear. Metal temperature and cooling rate —?

MR. KERR: Was the increase of pouring temperature from the middle to the end of the heat? Was that noted on all types of ladles and did you use the same temperature measuring system throughout?

MR. LILLIEQVIST: This subject was described in a recent paper by F. VonGruenigen and D. Lawther of our Granite City and East St. Louis Works respectively in a paper presented in April, 1950 on the subject of "Temperature in the Hearth and Ladle" before the National Open Hearth Conference. They inserted a 10% platinum, 10% rhodium-platinum thermocouple into the ladle approximately 1½ in. from the ladle lining and 9 in. from the top of the nozzle. By this means they could determine the temperature of the liquid steel during the entire pouring operation. At the beginning of the pouring operation the temperature is fairly high, then in the middle of the heat the temperature is the lowest. The temperature starts to increase again and at the end of the heat the temperature is approximately the same as in the beginning of the heat. This experiment has been carried out

on three different steels, Grade A, which is approximately 0.18-0.22 C, and approximately 0.70 Mn; Grade B steel, which is approximately 0.24-0.28 C, and 0.70 Mn; Grade C steel, which is approximately 0.30 C and 1.65 Mn. In all cases we obtained the same condition.

Furthermore, we also poured some fluidity test pieces at the beginning, middle and end of the heat. It was interesting to note that at the beginning and at the end of the heat the fluidity was the highest, while at the middle of the heat the fluidity was the lowest. This was consistent for all three compositions.

That was a 30-ton heat, using bottom-pour ladle.

MR. Kerr: Have you checked that on other types of ladles?
MR. Lilleqvist: Yes, we found the same thing on acid electric heats, using 6 to 7-ton ladles. However we did not run the same experiment as we did in this case. We intend to do that in the near future. We made some fluidity tests on the electric furnace heats and we also found that at the end of the heat we had the highest fluidity. That metal was also bottom-poured.

MR. Kerr: I had one other question in mind; however, your fluidity tests more or less cleared that up, at least for bottom-poured ladles. I was wondering whether the fact that your thermocouples were only 1½ in. from the ladle lining might not be responsible for the apparent temperature rise from the middle to the end of the heat.

When you tap the heat the thermocouple would of course show a rise. The thermocouple is close to the ladle lining (1½ in.) however, and so, after the initial rise, the relatively cool ladle lining would remove heat from the metal in its proximity and so show a temperature drop on the thermocouple until the ladle lining was nearly up to maximum temperature; thereafter, the thermocouple could show another rise due to the replacement of this chilled metal skin (a very small amount on a percentage basis) by normal conduction and convection within the ladle. In other words, it might be possible that the phenomenon noted was due to the placement of the thermocouple too near the ladle lining.

R. H. Jacoby (Written Discussion): ² The author of this paper is to be congratulated on the excellent contribution he has made to the foundry industry. The work, comprehensive as it is, certainly is thought-provoking and opens avenues for much additional work.

Of particular interest are the notes covering a comparison of attempts at temperature control previous to the installation of more accurate means of temperature determinations. The correlation of metal fluidity with metal temperature is very valuable, substantiating as it does, previous work along these lines.

Some two years ago in the foundry with which the writer is affiliated one of the factors to receive consideration during a quality-improvement program was the problem of metal pouring temperature. During ensuing shop discussions on this subject it became increasingly evident that there existed a wide lack of agreement between the melting, molding, pouring and quality control departments of the foundry. As a result of collaboration, a program was drafted as follows:

The melting department foreman was assigned responsibility for direct supervision in the pouring of all molds.

The molding and production control departments were held responsible for the scheduling of molds as heats in such a manner to insure as closely as possible a uniform arrangement of jobs as to pouring temperature requirements.

The heading and gating section became responsible in calling for metal pouring temperature ranges, i.e., pour hot, pour cold, etc. on all pilot jobs, where temperature variations were considered a factor.

Metallurgical department personnel were to be consulted where unusual conditions warranted it.

The cooperation in the application of the foregoing program was excellent, but immediately it became apparent that an important point had been overlooked. The melting department was not equipped with a basically sound tool for estimating

metal temperatures. Past history on the melting floor as regards determining relative tapping temperatures had made use of the seconds count for film formation in a 3-in. diameter core sand cup.

This test covered a range of more than 15 alloy steels ranging from a medium carbon steel to 25–12 stainless. Many of the higher alloy heats film forming characteristics varied in successive heats, i.e., a transparent film would form immediately but fluid metal would be observed underneath and vice versa. A decision was made to instigate on the melting floor as general practice the pouring of a fluidity spiral, adopting the core sand spiral mold as outlined in a paper by Messrs. Taylor, Rominski, and Briggs, "The Fluidity of Ingot Iron and Carbon and Alloy Cast Steels," Transactions, A.F.A., 1932. See Fig. D.

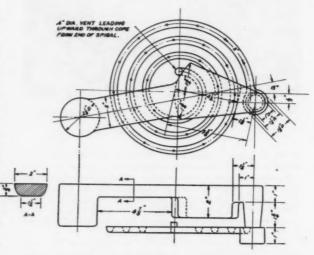


Fig. D-Fluidity spiral (Taylor, Rominski and Briggs.)

The relatively large amount of metal involved in pouring the standard spiral with pouring basin prompted a redesign of the spiral core mold eliminating the pouring basin. See Fig. E. Since adoption of the simplified spiral mold introduced the possibility of error due to speed and height of pour, standard practice for a period of 75 heats constituted pouring three spiral molds in succession (three separate dips in the bath).

Results of this work indicated a maximum variation of 8.5 per cent with the average at 5 per cent. It is worth noting that an occasional run out would account for a non-acceptable result, but as these miss tests were obvious when poured, the figures were not included.

As a result of this preliminary period, the melting department personnel had gained confidence in the test as regards the reproductibility of the results.

Recognition was taken of the necessity of close control of ladle preheat temperatures. New ladle drying and firing equipment was installed of a gas-fired alternate oil-fired design. Conditions of pre-heat time are standardized and rigidly adhered to in practice.

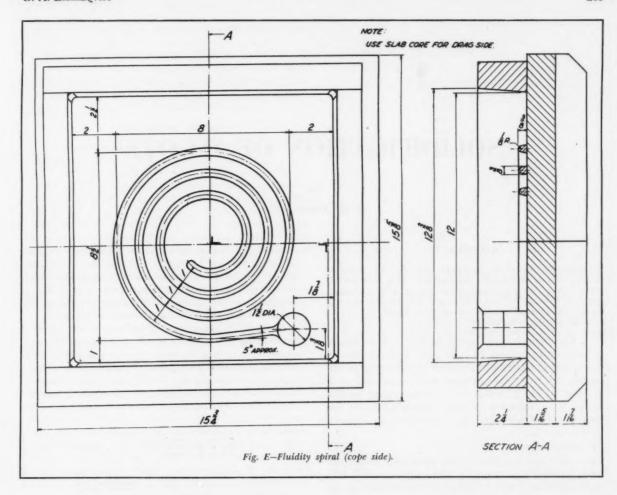
During this period correlations were being made between the number of inches spiral run and particular castings as regards fluidity requirements. Work progressed on the assumption that the pouring of molds at the lowest fluidity figure consistent with acceptable cast surfaces would result in better casting quality as regards solidity, cracks and penetration or sand inclusions.

With this information at hand the procedure in adopting the fluidity test consisted of pouring jobs or heats within specific inches of spiral run.

Practice on the furnace floor was established as follows:

After final alloy additions a seconds count film over cup test is taken in order to determine roughly whether sufficient temperature has been attained to pour the spiral test. The spiral test is poured imme-

² Metallurgist, Key Co., East St. Louis, Ill.



diately before tapping, and if out of range, is followed by another test after adjustments have been made to the molten bath. Representative fluidity runs would be 18 to 22 in. for the cold side, 22 to 26 in. for the intermediate or average range, and 26 to 30 in. for the high temperature requirements.

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Proof of success in any newly adopted practice lies in the final analysis, or end result, after a substantial period of use. The use of the fluidity spiral in determining pouring temperatures has been general practice at the Key Company for a period of 25 months.

The test is considered a success because it has made possible closer control of pouring temperatures contributing to a substantial reduction in scrap losses and a higher quality casting.

In view of our experiences over the past period of operations involving the use of the fluidity spiral and the correlation between fluidity and temperature, as substantiated in Mr. Lillie-qvist's paper, in our opinion the fluidity spiral test is to be recommended to the average small steel foundry as an inexpensive, practical means for temperature control in the manufacture of castings.

V. Paschkis (Written Discussion): The present paper is exceedingly interesting because it seems to present in very concise form valuable information regarding the thermal happenings in the ladle.

The writer would like to inquire regarding the following items:

1. In Fig. 12, the ladle temperature loss is shown vs the preheat time. It would appear that the temperature loss of the ladle for a given condition (for example, 2 hr preheat time) would not be constant: the metal poured out first would have a different temperature loss as compared with the last drops leaving the ladle. Is the loss shown in Fig. 12 "the average loss"? If so, would not the temperature loss as function of draining time be of significance? This would appear to be particularly the case if several small castings are poured from one ladle.

2. Still speaking of Fig. 12, it is obvious that the individual observed points scatter. This is inevitable because the preheat time for the ladle is possibly not sufficient to describe the temperature conditions. Depending on the length of interval between use of a ladle and of the conditions of the preheat means, the same preheat time may result in different temperature conditions in the ladle. It would be very significant if a graph similar to Fig. 12 could be plotted showing the temperature loss as function of the ladle temperature. In plotting such a curve it has to be remembered however, that the inside surface temperature of the ladle is not a complete indication of the ladle behavior. The temperature gradient through the lining of the ladle would also have to be known.

3. In addition to the ladle studies it would be interesting to study the temperature loss in pouring from the furnace into the ladle and from the ladle into the casting. Both drops are probably not negligible and depend among other things on the diameter of the stream of the molten steel.

4. The difference in observed temperature between the Rayotube and the optical instrument as shown in Fig. 14 should be a severe reminder not to accept all and any temperature measurements without critical review of the technique applied.

⁸ Technical Director, Heat and Mass Flow Analyzer Laboratory, Columbia University, New York.

SOLIDIFICATION OF INGOTS'

By

B. H. Alexander**

ABSTRACT

Solidification curves for duralumin were obtained at various pouring temperatures for two different size molds. The general shape of these curves is demonstrated. The effect of overheating upon the solidification curves and upon the length of the columnar crystallization zone is shown. The significance of the dumping method is discussed.

SOLIDIFICATION OF LIQUID METALS in ingot molds is basically a problem of heat flow. Complexities of this problem and the difficulties encountered in obtaining an analytical solution have been well summarized by H. A. Schwartz.¹ An expression for the variation in thickness of solid metal with time has been given by Field² and the formula which is known by his name is:

 $D = k\sqrt{t}$

where D is the thickness of the solid layer formed in time t and k is a constant. This equation, which is represented by curve I in Fig. 1, was derived on the basis of heat flow calculations in a semi-infinite mass and consequently cannot be expected to apply strictly to an ingot of finite dimensions. Schwarz³ and Tageev and Gulyaev⁴ have shown by heat flow calculations in which the geometry of the mold is taken into account that the parabolic relationship obtained by Field will only be obeyed in the first stages of freezing of ordinary ingots. In the later stages, freezing will progress faster than would be expected according to Field's equation. We would expect the course of freezing then to follow a curve such as II in Fig. 1.

The principal investigations of ingot solidification have been summarized in Table 1. It can be seen that the majority of the work confirms the solidification curve II in Fig. 1. The fact that the first three investigations listed in Table 1 failed to confirm this type of solidification curve may be due to the difficulty involved in obtaining good measurements in the latter

stages of freezing by the so called "dumping" method. Of the remaining investigations listed in Table 1 only that of Spretnak¹² is supposedly in disagreement with the behavior expected according to curve II in Fig. 1. Thus we see that there is still some doubt as to the correct form of solidification curves of ingots.

There is also considerable doubt as to the effect of overheating of the liquid metal upon the solidification curves. According to heat flow calculations,² overheating of the liquid metal before pouring should cause an increase in the total freezing time. Observations on this point are meager. However, Spretnak¹² reports that overheating up to a certain limit has no

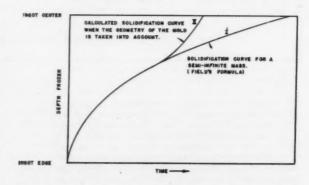


Fig. 1—Shape of solidification curves based on heat flow calculations.

TABLE 1-Investigations of Ingot Solidification

| Investigator | Confirms | Method | Material |
|---------------------------------|------------------|------------------------|--------------|
| Nelson ⁵ | Curve I, Fig. 1 | Dumping | Steel |
| Leitner ⁶ | ** | ,, | 22 |
| Chipman & Fondersmith | ** | ** | ** |
| Matuschka [®] | Curve II, Fig. 1 | 22 | 99 |
| Heggie ⁹ | 11 | ** | Stearic Acid |
| Tageev & Gulyaev | ** | Temperature | |
| | | Measurements | \$ |
| Zhegalov & Tagaev ¹⁰ | ** | 99 | 27 |
| Paschkis ¹¹ | 99. | Electrical Analogue | 91 |
| Spretnak ¹⁸ | ? | Dumping | 99 |

* This paper represents a portion of a thesis presented to the graduate committee of Carnegie Institute of Technology in partial fulfillment of the requirements for the D. Sc. degree.

** Formerly Assistant Professor of Metallurgy and Member of the Staff of the Metals Research Laboratory, Carnegie Institute of Technology. Presently with Sylvania Electric Products Inc., Bayside, L. I., N. Y. ne

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effect on the total time necessary for complete solidification.

The present work was undertaken to determine the true shape of the solidification curves and the effect of overheating upon these curves. At the same time the effect of overheating upon the length of the columnar crystallization zone was determined.

Experimental Method

The conventional dumping method was used to determine the rate of solidification. Duralumin, (Al, 4 Cu, 0.5 Mg, 0.5 Mn) was used in these studies because of the ease of melting and handling. Also, because of its lower melting point, the overheating could be varied over a wider range, with more accurate temperature control than hitherto obtained in the previous investigations with steel. The liquidus temperature of duralumin is 641 C and the solidus is 512 C.

Two split cast iron molds of different size were used. The larger mold was 6 in. x 6 in. inside dimensions at the top, tapered to 5 in. x 5 in. at the bottom, and was 12 in. deep with walls 2 in. thick at the top and 2.5 in. at the bottom. The smaller mold was 2.4 in. x 2.4 in. inside dimensions, 7.5 in. deep, with walls 0.7 in. thick.

Duralumin alloy was melted (without flux) in an electric furnace of 100-lb capacity for pouring the larger ingots, and in a gas muffle furnace for the smaller ones. After the alloy had been melted and heated to the desired pouring temperature, which was measured by immersing a chromel-alumel thermocouple directly into the melt, it was poured rapidly into the mold, which in all experiments was held at room temperature. It took 5 sec to fill the larger mold and 2 sec for the smaller one. After holding for a determined time interval the mold was overturned and the unfrozen liquid poured out. The solid crust remaining was sectioned vertically and the thickness was measured at mid-height. Several of the ingot sections thus obtained were macro-etched and the length of the columnar zone of crystallization was measured.

This method of determining the rate of ingot solidification does not give accurate results for very short holding times, where the thin solid crust is sometimes damaged during dumping, nor at very long times where very little liquid is left in the ingot center and does not pour out readily. The times necessary to form a solid crust clear to the ingot center were determined by measuring the time necessary for the pipe to form after filling the mold.

Discussion of Results

Solidification curves obtained are shown in Fig. 2 and 3 where the depth frozen is plotted against the square root of the holding time. If the data obeyed Field's equation this type of plot should give a straight line. It can be seen that none of the curves in Fig. 2 or 3 are straight lines from the beginning to the end of freezing. The data for the 2.4 in. x 2.4 in. ingot (Fig. 2) can be approximated by straight lines only for the first portion of the freezing process, although these straight lines do not pass through the origin. In the later stages of freezing there is a deviation from these

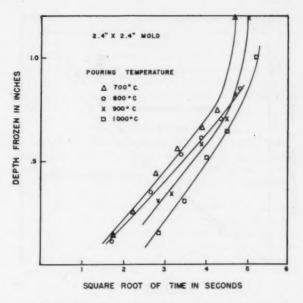


Fig. 2-Solidification curves for duralumin obtained at various pouring temperatures (2.4 in. x 2.4 in. mold).

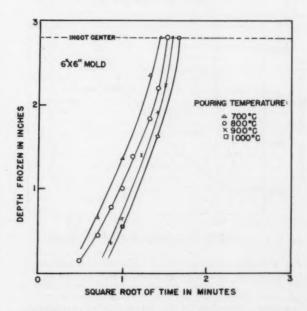


Fig. 3—Solidification curves for duralumin obtained at various pouring temperatures (6 in. x 6 in. mold).

straight lines indicating a more rapid freezing than is expected from Field's equation.

Solidification curves shown in Fig. 3 for the larger 6 in. x 6 in. ingot can not be represented by a straight line for any extended portion of the freezing interval. These curves also show a marked upward inflection near the end of freezing as would be expected according to the mathematical treatments of Schwarz² and Tagaev and Gulyaev.⁸

Increasing the pouring temperature shifts the solidification curves to the right, that is, it causes a delay in the initiation of freezing and increases the total freez-

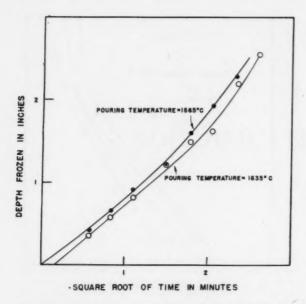


Fig. 4—Solidification curves for steel at two pouring temperatures in 6 in. x 6 in. mold. The data are taken from Spretnak.¹²

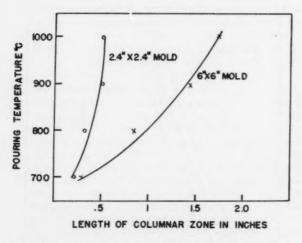


Fig. 5-Variation of length of columnar crystallization zone with pouring temperature for duralumin.

ing time. This shift of the curves due to overheating is of the same order as can be calculated from the thermal properties of the metal, if we assume that the delay in the beginning of freezing is caused by the removal of the sensible heat of the liquid metal. There is very little experimental data in the literature on this effect, although Spretnak12 reports that there is no change in the total time necessary for freezing of steel ingots when the pouring temperature is changed from 1565 to 1635 C. The results of Spretnak have been replotted in Fig. 4, and it can be seen that there is a small shift in the curves due to overheating. This shift is one of the same magnitude as would be caused by the rather small overheating of 70 C. It may also be observed that the shape of the solidification curves shown in Fig. 4 for steel is similar to those obtained

for duralumin. There is an upward inflection of the curves in the later stages of freezing indicating a more rapid freezing rate than expected according to Field's equation in this range.

We may conclude that this type of solidification curve is general for normal ingot shapes, and that overheating the liquid metal causes a shift of the curves to the right resulting in longer freezing times.

Columnar Crystallization Zone

Length of the columnar crystallization zone was measured on several ingots at each pouring temperature, and the results are shown in Fig. 5. It can be seen from these curves that there is a continuous increase in the length of the columnar zone with increasing pouring temperature. This effect has been observed before with pure aluminum¹³ but has never been adequately explained.

It seems that this may be due simply to undercooling of the liquid before freezing begins. It is well known that overheating a liquid metal results in more undercooling. This effect for pure aluminum is shown in Fig. 6 taken from the work of Horn and Masing.14 The greater the undercooling the greater will be the velocity of growth of the solid crystals which finally form. Thus, by overheating the liquid metal we decrease the rate of nucleation and the metal undercools to such an extent that the crystals that finally form can grow at a very high velocity. This would then lead to a more extensive columnar zone. The similarity of the effect of overheating on the length of the columnar zone (Fig. 5) and on the extent of undercooling (Fig. 6) supports this hypothesis. It would be expected that the undercooling of the metal in the

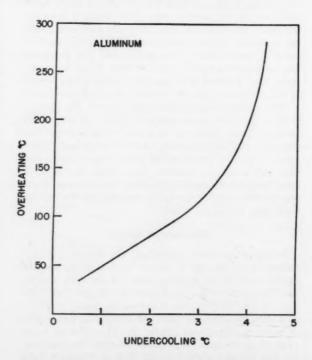


Fig. 6-Effect of overheating of the liquid on undercooling of liquid aluminum (after Horn & Masing14).

mold would be of short duration before the dendritic skeletons of the columnar zone had formed and would consequently be difficult to detect experimentally. Siegel,15 however, has detected pronounced undercooling of steel in ingot molds.

Dumping Method

It should not be assumed that the solidification curves obtained by the dumping (or bleeding) method express reliably the volume or mass velocity of freezing. This method only approximately defines the depth of crystal growth. The reason for this is that the solid crust does not grow with a plane interface that would allow complete separation of liquid and solid upon overturning the partially frozen mold. The solid crust is composed of a fine network of dendritic crystals which are capable of trapping considerable liquid in their interstices. Thus, the crystal structure of the metal can influence the solidification curve. Metals such as bismuth and antimony form solid crusts composed of comparatively coarse crystals which trap very little liquid, while metals such as aluminum and iron which form very fine dendritic structures will trap more liquid. This effect is exaggerated in an alloy with a great difference between the liquidus and solidus, for in this case the dendritic network will be more extensive, and more liquid will be trapped.

To demonstrate this point a solidification curve was obtained for pure aluminum using the same mold (6 in. x 6 in.) as was used for the duralumin alloy. The pouring temperature was 900 C and the mold was held at room temperature. Compared to duralumin, aluminum has a lower heat of fusion, a lower specific heat, and a greater thermal diffusivity, all of which should result in a shorter freezing time. The solidification curve obtained is shown in Fig. 7 with that of the duralumin alloy which was poured at the same

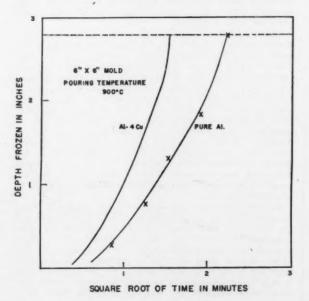


Fig. 7-Comparison of solidification curve of pure aluminum with duralumin at the same pouring temperature.

temperature. It can be seen that, contrary to the expectations from the thermal properties, the pure aluminum required a longer time to freeze. This indicates that the duralumin alloy retained more liquid upon dumping and gave an apparently shorter solidification time. This means, as stated before, that the dumping method does not give a true measurement of the volume or mass velocity of freezing.

Conclusions

1. Solidification curves of ingots do not obey Field's equation over the whole range of freezing. In the later stages there is a deviation resulting in a more rapid rate of freezing than would be expected, according to Field's equation.

2. Overheating of the liquid metal before pouring causes a delay in the beginning of freezing and an increase in the time necessary for complete solidification.

3. The dumping method does not give a measure of the volume or mass velocity of freezing for ordinary metals and alloys.

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DISCUSSION

Chairman: H. A. SCHWARTZ, National Malleable & Steel Castings Co., Cleveland.

Co-Chairman: E. C. TROY, Foundry Engineer, Palmyra, N. J. W. S. Pellini (Written Discussion):1 Dr. Alexander's paper

represents an important contribution to the knowledge of the mechanics of solidification. It is particularly interesting to note that the parabolic relationships postulated by Fields are shown to be valid only during the initial course of solidification. As he points out theoretical considerations preclude the validity of parabolic relationships to complete freezing. Dr. Paschkis' analogue predictions are in agreement with mathematical theory and with Dr. Alexander's results in indicating hyperbolic relationships during last stage solidification. Our own experimental results conducted by H. F. Bishop utilizing thermal analysis methods on steel and aluminum ingots likewise have shown a gradual change from parabolic to hyperbolic curve forms. The change is most definite after approximately 0.6 of the solidification wall has been developed holding for both "start of freeze" and "end of freeze" conditions. Dr. Alexander shows a similar infection at approximately the same fractional thickness point.

The apparent lack of superheat effect indicated by Spretnak has been rectified to show displacement towards later solidification times due to superheat. The data presented for the aluminum alloy definitely show the same effect. Our own thermal analysis results has likewise indicated a similar effect and approximately of the same degree. Inasmuch as Dr. Alexander's curves are not extrapolated to zero depth no inference can be drawn as to the nature of the contribution of superheat. It is noted that rates of solidification are not a factor inasmuch as the actual plots indicate similar rate for the superheat displaced curves. Would Dr. Alexander postulate on the process by which superheat effects the displacement? If the curves are simply extrapolated to zero time according to the simple parabolic form these must then indicate delay times as postulated by Field. On the other hand if asymptotic approach to the zero time is assumed parabolic rates are then invalid for first stage freezing.

All of this of course must remain in the nature of postulates since, as pointed out by Dr. Alexander the solidification curves obtained by dumping tests are inherently incapable of exact definition of the process. The erroneous conclusions which may be drawn by assuming too rigorous a validity to such tests is well demonstrated by the experiments comparing pure aluminum and a 4 per cent copper-aluminum alloy.

FORMATION OF COLUMNAR ZONES

The studies relative to the formation of columnar zones is quite interesting. We cannot agree, however, that undercooling conditions and related kinetics provide the sole answer to the effects noted. Undercooling certainly has an important effect for we know that cold pouring produces sufficient undercooling to generate seed crystals which may completely eliminate all evidence of columnar structures. Superheat effects are, however, lost rapidly after pouring. Thermal analysis studies have shown this to be so. Theoretical considerations also predict such a case for otherwise the liquid metal would be forced to hold an appreciable gradient. If superheat is effectively lost as a discrete but rapid rate it is difficult to understand the development of undercooling conditions at the freezing forefront which are operative over the entire range of solidification.

It is noted that the molds used are fairly thin, the 2.4 x 2.4 in. ingot having 0.7-in. walls and the 6 x 6-in. ingot molds having 2.25-in. (average) mold walls. This calculates to mold aspect ratios (ratio mold area to ingot area) of 1.5 and 2 respectively. In this respect the molds appear to be on the borderline of "sufficiency," defined as equivalence to infinite molds; or in other words a mold such that the initial high interface zone temperature is gradually decreased with time. "Insufficient" molds show a continuously increasing temperature in this zone with time regardless of the development of an air gap. The temperature at the interface of such "insufficient" or "borderline" molds is known to increase with superheat, hence should be considered to be higher throughout the solidification period the higher the

Alloys having a wide solidification range should be expected too sensitive to the thermal gradient conditions through the layer zone bounding the freezing forefront (liquidus temperature) and the plane of complete solidity (solidus temperature). If the

gradient is steep this zone should be narrow and undercooling in the liquid immediately to the fore of the freezing wave likely. If the gradient is gradual the solidification band should be expected to be quite broad and final freezing delayed due to segregation effects while the columnar tip which is solidifying at close to the initial liquidus temperature moves ahead relatively without hindrance.

With the contribution of "insufficient" mold the initially higher mold wall temperatures due to higher superheat effectively act to reduce the gradient conditions in the freezing zone hence to promote the natural growth of the dendrite inwards in its search for high freezing point liquid.

We know, for example, that the solidification band for alloys having a wide temperature range of solidification is exceedingly broad. A 0.60 per cent C steel cast in sand molds as 4-in. thick semi-infinite plates shows liquid very close to the surface at a time when the dendrite tips reach the center plane. Dendrite structures are quite pronounced in such cases. We know that such a process is also operative for nonferrous alloys prove to inverse segregation or "sweating."

The essential differences in our viewpoint therefore lie in the question of the kinetics of columnar growth. We view the requirements for pronounced columnar growth as a question of gradient conditions rather than kinetics. Seeding conditions occasioned by general undercooling represent a special case providing mechanical hindrance to the natural growth of dendrites. The macrostructural characteristics of steel slabs solidified in horizontal position substantiate these views. Such slabs solidify at the same rate from the bottom and top faces but show in many cases completely columnar structures extending from the top face to the center plane and, with the exception of thin columnar zone adjacent to the bottom surface, completely equiaxed structures developed from the bottom face. This is due to the formation of seed crystals which drop down on the growing dendrite of the bottom surface effectively stopping their continued growth. If the plate is reversed during solidification, columnar growth stops on what was the former top face and starts on the new top face. Obviously, the kinetics of growth have not been changed by this process.

R. L. Keller and E. A. Loria (Written Discussion):² It is interesting to have a criticism of the so-called "dumping method" as a means of defining the depth of crystal growth in metals at any given time during solidification. The writers are in agreement with Dr. Alexander's view that the crystal structure can influence the solidification curve obtained by the dumping method in small ingots since considerable quantities of liquid metal may be trapped in the interstices of the dendritic crystals when the ingot is dumped.

However, the writers feel that this conclusion has been determined only from a specific set of examples and is set forth in a much too general manner. Referring to Fig. 2, a duralumin ingot of 2.4-in. x 2.4-in. dimensions teemed at 900 C. is completely solid in less than 25 sec. Figure 3 shows that the same alloy cast at the same temperature in a 6-in. x 6-in. mold is completely solid in 2.25 min. Needless to say, dumping such ingots at specific time intervals would indicate a maximum effect in regard to trapping liquid metal and would also require great care to avoid errors in recording the true time of dumping. Owing to the rapid rate of solidification in these small ingots the equiaxed crystal zone would contain a fine network of dendrites which would offer more resistance to liquid metal flow than the larger dendrites formed in the equiaxed zones of larger ingots where the rate of solidification would be of a much lower order.

Recently, Queneau* discussed Marburg's conclusions based on experiments on the dumping of 32-in. x 32-in. ingots of 2 per cent nickel steel. These investigators concluded, in agreement with this work, that complete solidification of ingots occurred more rapidly than indicated by Field's equation. The writers wish to point out that in dumped ingots of such a large size as 32-in. x 32-in. there is not only less tendency to trap liquid metal during dumping due to the coarses dendritic crystal structure, but that any entrapped liquid metal

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² Mellon Institute of Industrial Research, Pittsburgh.

^{*} B. R. Queneau, "The Solidification of Steel Ingots," Symposium on Solidification of Metals and Alloys, AIME, February 1950.

would affect the final interpretation to a very minor degree compared to the small ingots cast by Dr. Alexander.

From Table 1 it would appear that a great divergence of opinion exists as to which shape of solidification curve steel follows during freezing, i.e., Curve I or Curve II in Fig. 1. Again, Queneau* has discussed the work of Marburg in this regard and has stated that vertical solidification in steel ingots occurs up to 65-in. height in big-end-up ingots over 18 in. wide, inferring that the effect of vertical solidification decreases as the width of the ingot decreases. If this is to be accepted, then it offers an explanation for the divergence of opinion in that transverse solidification would predominate in small ingots of say 6-in. x 6-in. cross section. Furthermore, transverse solidification alone tends to follow Field's equation, or Curve I. This simple analysis has been demonstrated in Nelson's paper on the solidification of steel in ingot molds.**

Early in this paper an explanation is offered for the fact that the first three investigations listed in Table 1 failed to confirm the accuracy of Curve II in favor of Curve I. The criticism was that this failure may have been due to the difficulty involved in obtaining good measurements in the latter stages of freezing by the so-called "dumping" method used in these early investigations. Perhaps, the author can explain how he arrived at this criticism when in the latter part of his paper he indicates that difficulty experienced in the use of the dumping method involves the trapping of liquid metal and that the result is an apparent reduction in the time for solidification than actually is required. If the first three investigations were affected by the errors cited in using the dumping method, then their results should show an unusually short time for solidification and would plot even to the left of Curve II. In contrast, since their results plotted on Curve I, they indicate nearly complete transverse solidification with very little help from the vertical component of heat abstraction and follow the explanation offered by the writers in their interpretation of Marburg's work.

B. F. Brown (Written Discussion): Beginning at least as early as the exploratory work by Briggs and Gezelius on the rate of solidification of steel castings,10 a great many studies have been made on the rate of solidification of pure metals and of alloys by the dumping method described in this paper. This method, while Dr. Alexander points out does not give a true measure of solidification rate, presumes that solidification is effected largely by the formation of a solid wall which increases in thickness with the passage of time.

Inasmuch as solidification of castings takes place with various departures from equilibrium conditions-notably undercoolingsolid crystals are probably forming well into the body of the casting by the time a wall forms strong enough to survive the dumping action, at least in ferrous metals and probably many others. Thus during solidification there is solid metal both in the wall (whose thickness is measured but which is incompletely solid) and in the form of grains completely detached from the walls which wash out with the liquid upon dumping.

The mechanism of solidification of a mass of metal is illustrated in Fig. A. That the detached grains illustrated in this figure do indeed form and that they in fact may account for a large fraction of the total solidification has been shown by the Melting and Metallurgical Committee of the BISRA® and by West.9 These investigators demonstrated that half of a steel casting of 4-in. thick section may solidify by the nucleation and growth of grains at random in the liquid.

The apparently anomalous solidification behavior of the duralumin and aluminum ingots in this paper might then be rationalized thus: If in the aluminum ingot there was occurring not only a wall growth but also a significant amount of solidification by nucleation and growth of solid grains at random throughout the liquid, the heat of fusion liberated by this random solidification would tend to counteract the cooling of the casting and thereby to decrease the rate of wall growth; when the ingot is dumped, many solidified grains would be dumped with the liquid. If there were less random nucleation in the duralumin ingot, this would promote a faster rate of wall growth again

because of the heat of fusion consideration. Under these circumstances the measured wall thickness of the alloy ingot could be the thicker, while at the same time the total mass of solid in the pure ingot might be the greater.

Dr. Alexander's own rationalization of this anomaly-that under different conditions the wall will contain different amounts of liquid in the interdendritic interstices-is plausible and illustrates one weakness inherent in the dumping method.

There are then two uncertainties involved in the study of solidification by the dumping method: (1) the uncertainty about how much of the wall, assumed solid, is actually solid, and (2) the uncertainty about how much of the dumped material, assumed liquid, is actually liquid. It is of pertinent interest that Fifield and Schaum,4 performing dumping experiments on cast iron, discovered that a 3-in. section, apparently solid throughout, was in fact incompletely solidified throughout and slumped under its own weight.

Dr. Alexander notes that he measured the wall thickness at mid-height, indicating presumably that the wall increased in thickness from top to bottom; such an increase could be caused by convection currents in the liquid metal, a phenomenon which has been well documented in steel and cast iron.

Even if it were a well defined parameter, "solidified thickness" (wall thickness) could not be ascertained with certainty at a given time during solidification either by mathematical computation or by analog experiments, since solidification either proceeds both by the growth of a wall and by random nucleation in the liquid-which are not wholly independent and which may vary in relative importance with varying superheat or presence of minute percentages of foreign substances. Neither does it appear promising-if indeed possible at all-to analyze the effects of thermal convection, which may at times be of great importance, by other than direct means.

From these considerations it would appear that where the limitations of their basic assumptions are involved, the conclusions drawn from dumping experiments and from heat flow studies based on these might well be accepted with reserve until they can be verified by direct observation of the mechanism of solidification. Such direct studies are now being made in the foundry research laboratory at M.I.T. under a grant from the A.F.S. Gray Iron Research Committee.

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- 3. D. R. F. West, "The Mechanism of Solidification of Horizontal Steel Castings," Journal of the Iron and Steel Institute, vol. 164, p. 182 (1950).
- 4. J. E. Fifield and J. H. Schaum, "Solidification Characteristics of Gray Cast Iron," Transactions, A.F.S., vol. 56, p. 382 (1948).

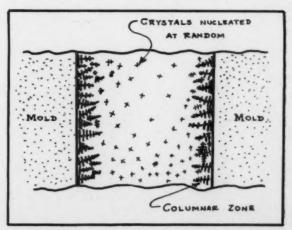


Figure A-Process of solidification of a mass of metal.

Research Associate, Massachusetts Institute of Technology, Cambridge.
 See references appended to Mr. Brown's discussion.
 L. H. Nelson, "Solidification of Steel in Ingot Molds," Transactions, ASM, vol. 22, pp. 193-226 (1934).

C. E. Sims (Written Discussion): This paper presents some very interesting data, painstakingly acquired, but the interpretation of the data seems definitely open to question and warrants further examination.

As pointed out by the author, Dr. Spretnak conducted some experiments on ingot solidification, which he concluded were in agreement with such other investigators as Field, Nelson, and Chipman in that the rate of ingot solidification varies with the square root of time from the onset of freezing. Spretnak also concluded that different pouring temperatures affect the start of freezing but do not change the time required to complete the freezing.

Some work carried on at Battelle independently, but at about the same time, pointed to similar conclusions. Thermocouples and cooling curves were used to determine the time of final solidification, but some dumping tests were also made.

Figure B of this discussion shows some typical results made on steel cast as an 8-in. x 8-in. ingot in a cast-iron mold. Two ingots were poured at a temperature of 2800 F and dumped at times of 6½ and 10 min, respectively, after pouring. Two more ingots were poured at 3000 F and dumped at times of 6 and 10 min. The thickness of the frozen shell was then plotted against the square root of time. Data so plotted should fall on a

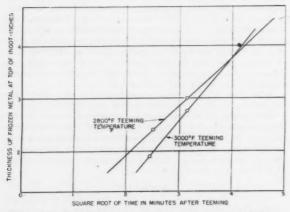


Fig. B-Process of freezing 8-in. square steel ingots poured at two different temperatures.

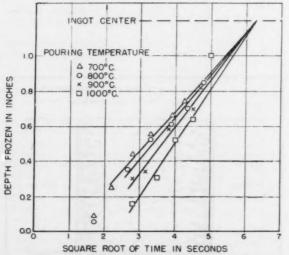


Fig. C-Solidification curves for duralumin obtained at various pouring temperatures (2.4-in. x 2.4-in mold).

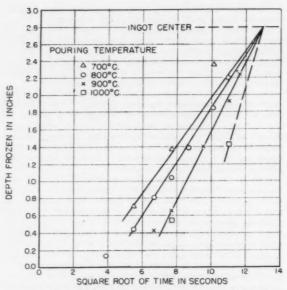


Fig. D-Solidification curves for duralumin obtained at various pouring temperatures (6-in, x 6-in, mold).

straight line if the principle of $D = K\sqrt{t}$ is correct.

Straight lines were drawn through the points for each pouring temperature. It may be seen that, whereas there is considerable difference in the thickness of the frozen shell at the end of 6 min because of pouring temperature, that difference is greatly decreased after 10 min. In other words, the ingot poured at higher temperature froze at a faster rate. The lines converge and cross at a wall thickness of 3.8 in. and a time of 4 min, or 16 min actual time after pouring. Thermocouples placed at the center of such ingots have shown an invarient time of close to 17 min for the completion of freezing, regardless of pouring temperature. The end of freezing, as shown by the cooling curve, is indicated by the black circle. Straight lines drawn through this point would fit the other data just as well. Such a close correlation is not merely fortuitous as was shown by other tests.

Dr. Alexander has plotted data from two different sized ingots cast from aluminum, in his Fig. 2 and 3. Through the points he has drawn curved lines, based primarily on the data for the end of freezing. These last data were obtained by observing the time when the pipe formed. This method has questionable accuracy, because the pipe must form before solidification is complete. The times are probably too short and should be rejected until proven by some more accurate determinations.

In Fig. C of this discussion, the data of the original Fig. 2 have been replotted, omitting the data obtained for the end of solidification. Now it may be seen that straight lines fit these data better than any curve. Furthermore, these straight lines converge at the ingot center. Only one point does not fall close to the straight line for the other data. It is a figure obtained very late in freezing where the accuracy is less, as the author mentioned, and it seems definitely out of place. The converging lines indicate that all ingots completed freezing in about 33 seconds, regardless of the pouring temperatures.

In Fig. D of this discussion, the data of the original Fig. 3 are plotted minus the data for the end of freezing. The data for the very short holding times may be considered erratic for the reasons mentioned by the author, or they may be considered to agree with the findings of Spretnak, that there are two intersecting parabolas and that these points fall on the first one.

The other data for the 800 and 900 F ingots fall almost perfectly on straight lines that intersect at the ingot center. Two of the three points for the 700 F ingots fall on a straight line that intersects at the same place. There are not enough points for the 1000 F ingots to fix a line very definitely. It is thus indicated that all ingots would complete freezing at the same time, which is approximately 23/4 min. The data appear to support the equation of Field and the conclusions of Spretnak.

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SOME EFFECTS OF DEOXIDIZING ADDITIONS ON FOUNDRY MALLEABLE IRONS

By R. W. Heine*

Introduction

THIS PAPER PRESENTS RESULTS of a continuation of previous investigation of the effects obtained when additions of deoxidizing elements are made to the melt of white cast irons used for malleabilizing purposes.

Effects of deoxidizing additions are dependent on the amount of oxygen residual in the melt at the time of the addition. There are of course other important factors such as temperature, type of addition, holding time, metal composition, and others. However, one major effect of the melting process exists in the percentage of oxygen which is introduced and remains in the white iron. The oxygen which remains in small residual percentages in the solidified iron may have a profound influence on many of the characteristics of the iron. Data for laboratory prepared irons have been reported in a recent paper1 showing that the nonmetallic inclusions in the iron, its graphite nucleating tendencies, and its rate of response to annealing are markedly altered when the melt is treated with deoxidizers. The original work is herein extended to irons melted under foundry conditions. An additional viewpoint is gained in that the variation in residual oxygen in foundry irons from heat to heat and in different foundries may be studied by the indirect method of deoxidizing additions.

Experimental Details

The white irons studied in this investigation were produced in three different malleable foundries all employing the cold-melt air furnace melting practice. In each foundry the metal was melted and finished by its own particular normal practice throughout the entire heat. Tapping of the heats from the air furnace was begun and continued as usual for pouring the normal foundry production. About 10 min after the beginning of the tap, pouring of tensile test bar molds was begun using 50-lb hand shank ladles of metal direct from the furnace. After standard bars had been cast, with no addition, a series was poured in which increasing amounts of deoxidizer, usually

aluminum, were added to the metal. Aluminum additions of 0.01 to 0.18 per cent were used. In one series, titanium additions were used as well as aluminum. The selected percentage of the deoxidizer was added to the empty ladle and then metal tapped into the ladle direct from the furnace. From each ladle so treated, a sprue test for mottling tendencies and a number of tensile bars were cast. In this way, test pieces were cast which varied in deoxidation from none in the standard bars to that produced by the maximum addition of deoxidizer. The castings were allowed to cool in the mold before being shaken out. A portion of the tensile test bars were then annealed in the same foundry where they were cast according to the usual heat treating cycle. While a greater number of heats were studied in this investigation, the data considered will be that taken from three heats in each of the three foundries. Chemical analyses of these heats and the magnitude of the deoxidation additions are reported in Table 1. In a number of cases, chemical analyses were made of the last bars poured as well as the first to determine whether significant changes in chemistry occurred during the pouring period. Such changes were not encountered.

Detailed study of the annealed and white iron test bars obtained by the aforementioned procedure was made to note in particular any relationships between the amount of the deoxidizing addition and the following:

 Effect of the non-metallic inclusions present in the iron.

2. Mottling tendencies.

- 3. Graphite nucleation and distribution of nodules.
- 4. Appearance of a new phase in the structure.

5. Physical properties.

6. Subcritical graphitizing characteristics of the white iron.

The above listed items are considered separately in the following sections.

Nonmetallic Inclusions

Deoxidizing treatments with aluminum produced pronounced changes in the nature and distribution of nonmetallic inclusions present in the as-cast foundry

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TABLE 1-CHEMICAL ANALYSIS, %

| Foundry | Heat No. | С | Si | Mn | P | s | C+Si | Oth | ners | | | Additions | 3 | |
|---------|-------------|------|------|------|-------|-------|------|---------------|----------|--------------------|--------------------|------------------|--------------|-------|
| A | 1 | 2.36 | 1.14 | 0.31 | 0.125 | 0.110 | 3.50 | Cr: | none | 0.010, 0.120, | 0.030, 0.160% A | 0.050, | 0.080, | |
| | 2 | 2.24 | 1.17 | 0.28 | 0.121 | 0.095 | 3.41 | ** | 99 | 0.010, 0.110, | 0.020, 0.140, | 0.040, .170% | 0.060, A1 | 0.080 |
| | 31 | 2.39 | 1.09 | 0.29 | 0.132 | 0.096 | 3.48 | ** | 99 | 0.010, 0.120, | 0.020, 0.150, | 0.040, 0.180% | 0.060, Al | 0.090 |
| | 32 | ** | 99 | ** | 99 | ** | 29. | ** | 29 | 0.015, 0.180, | 0.030, 0.240, | 0.060, 0.270% | 0.090, Ti | 0.120 |
| В | 1 | 2.47 | 0.98 | 0.33 | 0.130 | 0.120 | 3.45 | 0.084 0.01 | | 0.020, 0.120, | 0.040, 0.18% Al | 0.060, | 0.090, | |
| | 2 | 2.44 | 0.92 | 0.33 | 0.130 | 0.116 | 3.36 | 0.094 0.01 | | 0.010, 0.090, | 0.030, 0.130% A | 0.050, | 0.070, | |
| | 3 | 2.53 | 0.97 | 0.34 | 0.120 | 0.135 | 3.50 | 0.10 0.01 | Cu Cr | 0.010, 0.050, | 0.020, 0.080% A | 0.030, | 0.040, | |
| C | 1 | 2.75 | 1.12 | 0.31 | 0.155 | 0.069 | 3.87 | _ | | 0.010, 0.120% A | 0.020, 11 | 0.040, | 0.080, | |
| | 2 | 2.61 | 1.15 | 0.27 | 0.140 | 0.066 | 3.76 | | _ | 0.010, 0.060, | 0.020, 0.080% A | 0.030, | 0.040, | |
| | 3 | 2.79 | 1.18 | 0.37 | 0.137 | 0.060 | 3.97 | _ | | 0.010, 0.060. | 0.020, 0.080% A | 0.030, | 0.040, * | |

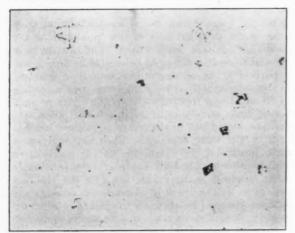
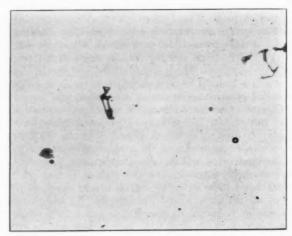


Fig. 1A (Left), 250X-Large, irregular, random sulphides, Type I. Fig. 1B (Right), 750X-Sulphides, free



silicates and silicates within sulphides. Silicates identified with polarized light.

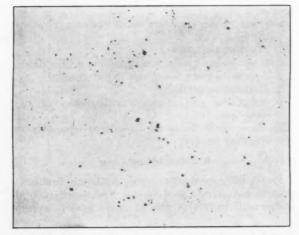
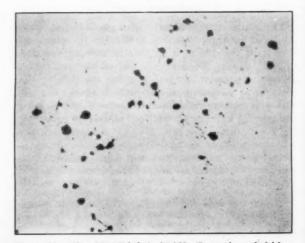
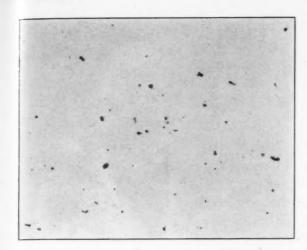


Fig. 2A (Left), 250X-Small eutectic-type sulphides,



Type II. Fig. 2B (Right), 750X-Eutectic sulphides.



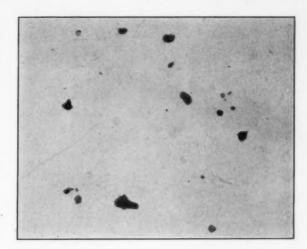


Fig. 3A (Left), 250X-Random Type III inclusions; some duplex sulphides and some alumina. Fig. 3B (Right), 750X-Type III duplex sulphides.



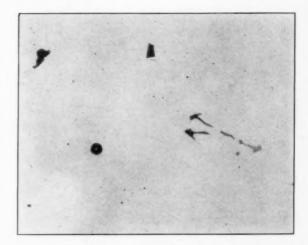


Fig. 4A (Left), 250X-Type I sulphides and silicates in Heat B-1, no aluminum addition. Fig. 4B (Right), 750X

-Type I silicates and sulphides in Heat B-1.

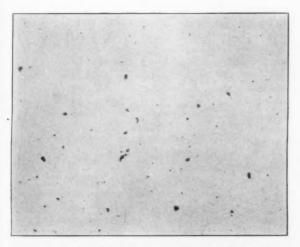
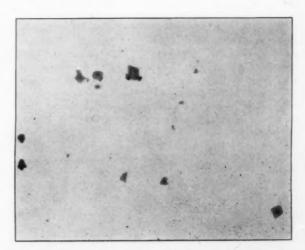
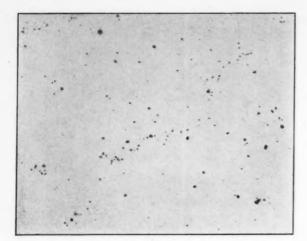


Fig. 5A (Left), 250X-Type I sulphides in Heat B-1 after 0.02 per cent aluminum addition. Fig. 5B



(Right), 750X-Type I sulphides in Heat B-1 after 0.02 per cent aluminum addition.





white irons. The trend of these effects with increasing magnitude of deoxidizing addition was the same in the foundry melted irons as that occurring in irons melted in the laboratory and described by the author in Ref. 1. Three basic types of inclusions were observed upon metallographic examination of the tensile test bars. These are shown in Fig. 1, 2 and 3. They are similar to the Type I, II, and III inclusions so well known in cast steels.2,3 Photomicrographs at magnifications of 250X and 750X are shown for each type in Fig. 1, 2 and 3. Marked changes in type, distribution, and size may be noted. All inclusion study samples were taken from the tensile bars at a position midway between the shrink-bob gate and the shoulder of the necked-down portion on a longitudinal section. Type I inclusions, Fig. 1, are defined as random sulphides and silicates and these occur in irons where no deoxidizing treatment is employed. The sulphides are characteristically large and often show a tendency to be sprawly in shape; Fig. 1A. All the cast irons with Type I inclusions examined by the author contained silicates as well as the sulphides; Fig. 1B. Type II inclusions which develop when the metal is virtually deoxidized, are described as eutectic sulphides occurring in chains or galaxies. They are shown in Fig. 2. Silicates appeared to be absent when Type II inclusions were present. The eutectic sulphides were

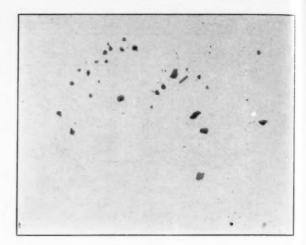


Fig. 6A (Left), 250X-Type II sulphides in Heat B-1 with 0.09 per cent aluminum addition. Fig. 6B (Center), 750X-Type II galaxy sulphides in same heat. Fig. 6C (Right)-Type II sulphides in dendritic stringers. Same heat as Fig. 6A.

often dispersed in a pattern outlining the dendritic nature of the grains or in galaxy groupings in the metal eutectic cells. They are thus considered to be precipitated late in the freezing process as is the case with the same type of inclusions in cast steels. Type III inclusions, Fig. 3A and 3B are developed when the amount of deoxidizer exceeds that necessary to produce Type II inclusions. Type III inclusions are random sulphides, often showing a duplex structure, Fig. 3B, and with alumina usually being present. Types I, II, and III inclusions were observed whenever the proper range of deoxidizing additions was covered and they occurred in all of the heats studied.

As an example of the inclusion changes which occur when a particular heat is deoxidized with increasing percentages of aluminum, Heat 1 from Foundry B will be considered. The original as-cast inclusion distribution is shown in Fig. 4, A and B. Type I sulphides and silicates are evident. An addition of 0.02 per cent aluminum produced the inclusions shown in Fig. 5. A more uniform polygonal shape occurs but they are still basically Type I. However, silicates were no longer present. With increasing percentages of aluminum, the inclusions become finer, being at first randomly dispersed but finally assuming the eutectic type of distribution. In this heat, deoxidation with 0.06 to 0.09 per cent aluminum was required to produce the Type II inclusions shown in Fig. 6, A, B and C. Galaxy and dendritic groupings are shown in Fig. 6B and 6C. Larger additions, 0.12 and 0.18 per cent aluminum caused a return to the random sulphides characteristic of Type III and shown in Fig. 7. About 0.03 per cent aluminum treatment greater than that required to produce Type II inclusions was capable of causing Type III inclusions to appear in most heats. Duplex Type III inclusions and alumina were most commonly present when the addition was about 0.06 per cent aluminum in excess of that required to pro-

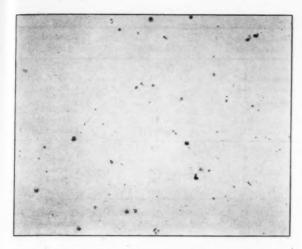
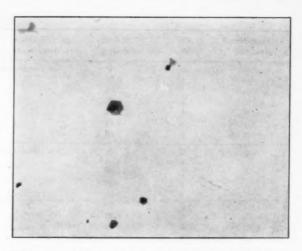
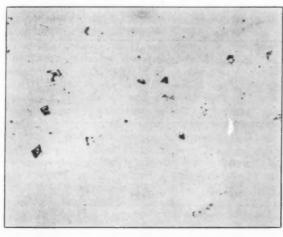


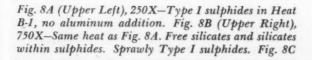
Fig. 7A (Left), 250X-Type III sulphides in Heat B-I with 0.12 per cent aluminum addition. Fig. 7B (Right),

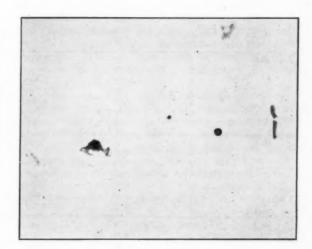


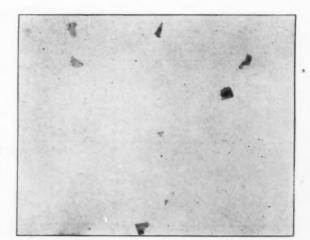
750X-Type III sulphides in Heat B-1 with 0.12 per cent aluminum addition.











(Lower Left), 250X—Type I sulphides in Heat C-1, no aluminum addition. Fig. 8D (Lower Right), 750X—Same heat as Fig. 8C. Silicates occur only within sulphides. Type I sulphides of more uniform shape.

TABLE 2

| Foundry Heat No. | Bar No. | Al Addi- tion, % | Residual Aluminum, % | Inclusion Type | Mottling | Residual Phase |
|---------------------|------------|---------------------|-------------------------|-------------------|----------------------|--------------------------|
| | | | | | | |
| A-1 | 0 | 0 | Not Determined | I | No | None |
| | 1 | 0.01 | | I | No | None |
| | 2 | 0.03 | | I | No | None |
| | 3 | 0.05 | | I | No | None |
| | 4 | 0.08 | | II | No | Slight |
| | 5 | 0.12 | | 1118-111 | No Definite | Increasing Increasing |
| | 6 | 0.16 | | | | |
| A-2 | 0 | 0.01 | Not Determined | I | No No | None None |
| | 1 2 | 0.01 | | I | No | None |
| | 3 | 0.02 | | ī | No | None |
| | | 0.04 | | ī | No | None |
| | 5 | 0.08 | | 1811 | No | Slight |
| | 6 | 0.11 | | II | No | Increasing |
| | 7 | 0.14 | | III | Definite | Increasing |
| | 8 | 0.17 | | III | Definite | Increasing |
| A-3 | 0 | 0 | Not Determined | I | No | None |
| 14-3 | 1 | 0.01 | TO DECEMBE | Î | No | None |
| | 2 | 0.02 | | Î | No | None |
| | 3 | 0.04 | | Ī | No | None |
| | 4 | 0.06 | | I | No | Trace |
| | 5 | 0.09 | | II | No | Slight |
| | 6 | 0.12 | | 1118111 | No | Definite |
| | 7 0.15 III | | | | Definite | Definite |
| | 8 | 0.18 | | III | Definite | Definite |
| B-1 | 1 | 0 | Trace | I | No | None |
| - | 2 | 0.02 | Trace | I | No | None |
| | 3 | 0.04 | Trace | I | No . | Trace |
| | 4 | 0.06 | 0.018 | 18:11 | No | Definite |
| | 5 | 0.09 | 0.063 | II | No | Increasing |
| | 6 | 0.12 | 0.10 | III | Slight | Increasing |
| | 7 | 0.18 | 0.16 | III | Definite | Increasing |
| B-2 | 1 | 0 | Trace | 1 | No | None |
| | 2 | 0.01 | Trace | I | No | None |
| | 3 | 0.03 | Trace | I | No | Trace |
| | 4 | 0.05 | 0.028 | 18:11 | No | Definite |
| | 5 | 0.07 | 0.040 | II | No | Increasing |
| | 6 | 0.09 | 0.076 | III | Slight | Increasing |
| | 7 | 0.13 | 0.124 | III | Definite | Increasing |
| B-3 | 1 | 0 | Trace | I | No | None |
| | 2 | 0.01 | Trace | I | No | None |
| | 3 | 0.02 | Trace | 1 | No | None |
| | 4 | 0.03 | Trace | 1 | No | Definite |
| | 5 | 0.04 | Trace; closer to 0.01% | 18:11 | Definite | Increasing |
| | 6 | 0.05 | 0.026 | II | Definite | Increasing |
| | 7 | 0.08 | 0.046 | II | Definite | Increasing |
| | Sprue Test | 0.12 | | | Gray | |
| C-1 | 0 | 0 | Not Determined | I | No | None |
| | 1 | 0:01 | | 1 | No | None |
| | 2 | 0.02 | | 1811 | No | Trace |
| | 3 | 0.03 | | 18:11 | No | Slight |
| | 4 | 0.04 | | II | Slight | Definite Increasing |
| | 5 | 0.06 | | III | Definite Definite | Increasing |
| | 6 | 0.08 | | | | |
| C-2 | 0 | 0.01 | Not Determined | I | No No | None None |
| | 1 | | | Ī | No | None |
| | 2 | 0.02 | | 18:11 | No | Slight |
| | 3 | 0.03 | | II | No | Definite |
| | 4 | 0.04 | | III | Definite | Increasing |
| | 5 | 0.06 | | III | Definite | Increasing |
| | | | Not Determined | | | None |
| C-3 | 0 | 0.01 | Not Determined | I | No No | None |
| | 2 | 0.02 | | i | No | None |
| | 3 | 0.03 | | Î | No | None |
| | 4 | 0.04 | | 18:11 | No | Slight |
| | | | | II | Slight | Increasing |
| | 5 | 0.06 | | | | |

TABLE 2 (CONT.)

| Foundry Heat No. | Bar No. | Al Addi- tion, % | Residual Aluminum, % | Inclusion Type | Mottling | Residual Phase |
|---------------------|------------|---------------------|-------------------------|-------------------|---|-------------------|
| A-32 | 0 | 0 . | Residual | I | No | None |
| Titanium | 1 | 0.0150 | Titanium | I | ' No | None |
| Treated | 2 | 0.030 | Not Determined | - I | No | None |
| | 3 | 0.060 | | I | No | None |
| | 4 | 0.090 | | I | No | None |
| | 5 | 0.120 | | II Ti | No | None |
| | 6 | 0.180 | | II Ti | No | None |
| | 7 | 0.240 | | II Ti | No | None |
| | 8 | 0.270 | | П | Definite (very fine mottle dis- persion) | Definite |

duce Type II. This condition is shown in Fig. 7A and 7B for this particular iron. Temperature was found to be a factor of major importance in obtaining Type III inclusions of a duplex structure. A temperature in excess of 2800 F was necessary to obtain duplex Type III inclusions. While the inclusions described above are those obtained from one heat, it was observed that all the other heats followed the same trend but with differences in the amount of aluminum required to produce the Type II and III inclusions.

Appearance of Type II inclusions has been associated with virtually complete deoxidation in cast steels.^{2,3} Since percentage of oxygen in the melt may vary from heat to heat, the amount of aluminum required for deoxidation and Type II inclusions will vary. Pronounced differences of this kind were found to exist in the heats investigated and also when heats from the three foundries were compared. These data along with other data that will be referred to later are summarized in Table 2. From Table 2 it may be noted that Type II inclusions were produced with additions ranging from 0.02 to 0.12 per cent aluminum in different heats at different foundries. Furthermore, it may be seen that Foundry A was consistently above

Foundry B in the amount of aluminum required for Type II inclusions. Also, Foundry A was the most consistent in its percentage of aluminum requirement for the eutectic sulphides. It would appear from these data, that Foundry A had achieved a higher and more reproducible degree of oxidation in its melting practice than B and C in the particular heats investigated; that is if inclusions are used as the test.

A distinct variation in residual oxygen in the melt from heat to heat is thus indicated by the inclusion study. This is a significant point because as pointed out later, similar variations were observed in other properties of the irons.

Certain characteristics of the inclusions deserve special consideration. The Type I inclusions offer some valuable information when the differences from heat to heat are noted. When a heat is sufficiently oxidized, inclusions such as those shown in Fig. 1 and 4 are observed. However, a less oxidized heat always revealed a different distribution of sulphides than that in the aforementioned figures. The sulphides in heats of higher and lower degree of oxidation are illustrated in Fig. 8A, 8B and 8C, 8D respectively. It may be noted that in a more oxidized heat, Fig. 8A and 8B,



Fig. 9A (Left), 750X—Silicate in massive carbide area, Heat B-1, no aluminum addition. Fig. 9B (Right), 750X—Dark and light silicates in massive carbides,



arbide area, Heat B-1. The two small white circles are identified as silicates by means of polarized light. They present a yellowish translucent color in white light. Both samples etched with picral.

the sulphide inclusions are quite sprawly as contrasted with the more rounded and uniform sulphides in the less oxidized heat, Fig. 8C and 8D. Furthermore, the more oxidized heat shows free silicates as well as silicates within the sulphides as shown in Fig. 1B. In a less oxidized heat, only the silicates within the sulphides appear. The free silicates in the more oxidized heats are of special interest since they appear to exist mainly in the carbide areas. The latter effect is illustrated in Fig. 9A and 9B. As pointed out in Fig. 9, the free silicates differ in color from a light translucent yellow to almost a black. They were identified metallographically as silicates by means of polarized light. The occurrence of the two different dispersions of silicates suggests a solidification mechanism whereby sulphides are initially nucleated during freezing by silicates. In a more oxidized heat, more silicates may be formed than are necessary to nucleate and permit growth of the sulphides so that free silicates may exist and form down to the eutectic solidification range. In all white irons examined by the writer, a considerable number of the sulphides contained silicates but only in the more oxidized heats (from Foundries A and B, for example) were free silicates present. A heat was judged to be more or less oxidized by the amount of aluminum required to produce Type II inclusions and by the sprue test and other data described later in the paper.

Another nucleation phenomenon was observed in the Type II inclusions. It appears that the sulphides occurring in samples with these inclusions are also nucleated by other substances during freezing. Figure 10 shows sulphides having a nucleus of what appears to be aluminum oxides. These also may be observed in Fig. 2B and Fig. 6. The size of these nuclei varies from a tiny dark speck to the relatively large areas shown in Fig. 10. Thus, it seems that whereas Type I sulphides are nucleated during freezing by silicates, Type II inclusions form in eutectic galaxies that may be deposited on aluminum oxide nuclei. This may account for the relative absence in the white iron of aluminum oxide galaxies or clusters which are often

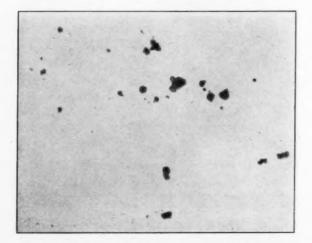


Fig. 10, 750X-Duplex inclusions showing sulphides with core of dark aluminum oxide.

reported in deoxidized cast steels. Oxides are evidence of the conversion of the oxygen in the melt to stable aluminum oxides whose amount depends on the percentage of oxygen in the melt. Therefore, it is not a coincidence that this condition was much less evident in the less oxidized irons from Foundry C.

Gravimetric chemical analyses for residual percentage of aluminum were carried out in the case of heats at Foundry B. The results are listed in Table 2. The data appear to indicate that a small but definite residual percentage of aluminum is necessary to cause Type II inclusions to exist. This was also reported to be the case in the laboratory melted irons.1 According to these analyses, an addition of a minimum percentage of about 0.05 per cent was necessary to produce a definite residual percentage of unoxidized aluminum and Type II inclusions. The requirement of definite residual percentage of aluminum seems to agree with the similar case for Type II inclusions in cast steels reported in Ref. 2. The effect of aluminum additions on the change from Type I to Type II inclusions in white cast irons thus appears to be mainly the result of a deoxidizing action since the Type II inclusions do not develop until deoxidization progresses to the point where some residual aluminum is present.

Summarizing, it appears that additions of aluminum to molten white iron produce changes in inclusion distribution which are dependent on the magnitude of the aluminum addition and the initial degree of oxidation of the melt. Type I, Type II and Type III

inclusions may occur.

Rather broad comparison has been made in the foregoing paragraphs to the similarity of the inclusion types produced in aluminum-treated cast steel and white cast irons. There are, however, some differences which should not be ignored. For example, the Type I sulphides in cast steels are usually globular whereas those in the white irons have a skeletal shape. Furthermore, the Type II inclusions in deoxidized cast steel tend to be more filmy and follow a more definite grain boundary pattern. The aforementioned differences are thought to be principally due to the lower temperature of solidification and the mechanism of primary and eutectic solidification which occurs in white cast irons as contrasted with steels. Possibly a classification other than Type I, II, and III inclusions may ultimately be adopted to indicate inclusions peculiar to cast iron. For the present, however, the established designations for cast steel are used by the author to simplify reference to the basic types of inclusions in white cast irons.

Sprue Test

A sprue test was found to provide a means of evaluating the effects of deoxidizing treatments with aluminum. Test sprues about $1\frac{1}{2}$ to 2 in. in diameter and about 8 in. long were poured from a number of heats. Usually sprues were poured from each ladle at the same time as the tensile bars but they were also cast in some cases at the beginning of the tap to determine the working range of additions desirable. The test consisted simply of determining the percentage of aluminum required to produce definite mottling in the white iron sprue. The white iron and mottled

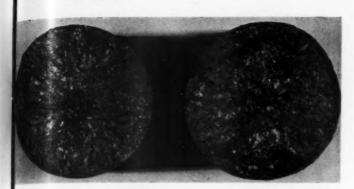


Fig. 11—Fracture of sprue test bars. Sample at left from Heat B-3 has a clear white fracture. Sample at right is from same heat but treated with 0.05 per cent aluminum and it shows definite mottling.

sprues are shown in Fig. 11. This test produced interesting results in revealing the variation in response to deoxidation of the iron from heat to heat, and foundry to foundry. Foundry A, for example, was consistent in requiring 0.14 to 0.17 per cent aluminum for mottle in the sprue. Foundry C, on the other hand, required much less, from 0.02 to 0.08 per cent, while Foundry B was intermediate. The test thus appears to be capable of checking on the degree of standardization attained in controlling the oxidation of the melt.

Correlation of the sprue test with inclusions was noted. Traces of mottle were occasionally noted when Type II inclusions were developed in the tensile test bars, but definite all-over mottling occurred when the Type III inclusions existed. These observations are summarized in Table 2 and the relationship between sprue mottling and inclusions seem positive. Mottling in the sprue test and in the castings usually occurred at about the same aluminum addition, but a few exceptions were noted.

It was found that the sprue test must be used under as carefully standardized circumstances as possible. Temperature of the metal is one of the most important factors. It was necessary to carry out the test with metal at the tapping or pouring temperature representative of the cast. Holding of the metal in the ladle with a temperature drop and its attendant reactions was found to influence the results. The data described herein apply to the particular conditions of this investigation, i.e., metal direct from the furnace and at the same temperature as the tapping tempera-

ture allowing for temperature drop in the ladle. A rising or falling temperature has been observed to alter the results, lower temperatures requiring less aluminum for mottling. Under standard conditions, the sprue test may serve as a useful test to the malleable melter in determining the uniformity of his practice with respect to the degree of oxidation which the melting practice has brought about in the melt.

Deoxidation with titanium was also capable of producing mottle in the sprue test. However, the mottle spots were so small in size that they were much more difficult to observe than those produced with aluminum and therefore the test was not so convenient.

Graphitization

Bars from each heat were given malleabilizing treatments in the foundry where they were cast. The normal cycle for each foundry was used, with the bars from a single heat being annealed as one group. The initial object of this procedure was to determine the effect of deoxidizing additions on graphite nucleation and distribution when the complete annealing cycle was held fixed or normal and not considered as a variable being investigated. The annealing cycle for each foundry is described in Table 3.

In the case of heats from Foundry A, deoxidation with aluminum affected graphitization in general according to the principles described in Ref. 1. Small additions, 0.01 to 0.03 per cent aluminum, caused a marked decrease in the number of graphite nodules in the treated iron as compared with the untreated iron. This effect is illustrated in fully malleabilized irons in a comparison of Fig. 12A and 12B; Fig. 12A showing the nodule distribution in the untreated iron and Fig. 12B showing nodule distribution in the same iron but having 0.01 per cent aluminum added. A further effect evident in comparing Fig. 12A and 12B is that the many small flake-like particles in Fig. 12A have disappeared in Fig. 12B as a result of the 0.01 per cent aluminum addition. This was a consistent effect of deoxidation with small amounts of aluminum and titanium in all irons investigated in Foundry A. Larger additions of aluminum than these which caused a nodule number decrease resulted in a marked increase in the number of graphite nodules. The latter effect is illustrated in Fig. 12C where an addition of 0.05 per cent aluminum produced as many or more. nodules as occurred in the untreated iron. As the deoxidizing addition is made still larger, to the point where Type II or III inclusions appear, the nodules become sprawly, flakes of graphite, and mottling appears in the structure. Figure 12D illustrates this

TABLE 3-ANNEALING CYCLE

| Foundry | Heating | Holding at First Stage Temperature | Cooling and Second Stage of Graphitization | Total |
|-------------------|--------------------|---------------------------------------|---|--------|
| A | To 1500 F in 25 hr | | 1700 to 1100 F | 196 hr |
| Pot annealing, | To 1700 F in 46 hr | 53 hr at 1700 F | in 97 hr | |
| Packed | | | | |
| В | To 1600 F in 45 hr | 45 hr at 1600 F | 1700 to 1100 F | 150 hr |
| Pot annealing, | | | in 60 hr | |
| Packed | | | | |
| C | To 1700-1720 F in | 13 hr at 1700- | Cool to 1200 F in | 54 hr |
| Electric Furnace, | 5 hr | 1720 F | 36 hr | |
| Not Packed | | | | |
| | | | | |

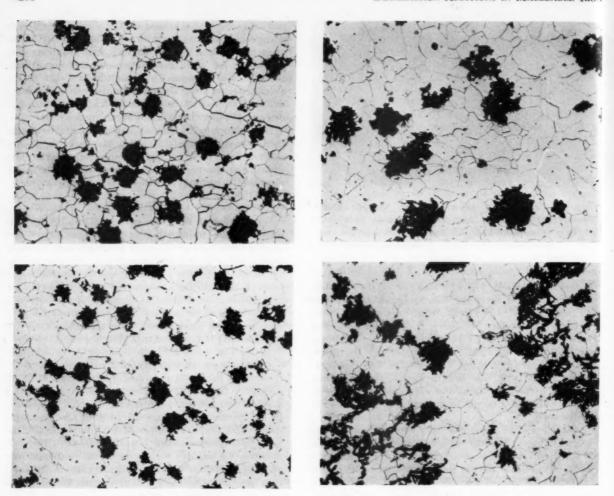


Fig. 12A (Upper Left), 150X, nital etch—Heat A-1, no aluminum addition. Graphite distribution in malleabilized iron showing many small graphite spots in addition to the nodules. Fig. 12B (Upper Right), 150X, nital etch—Heat A-1, 0.01 per cent aluminum addition. Same treatment iron of Fig. 12A. Much

smaller number of nodules and fewer small spots of graphite. Fig. 12C (Lower Left), 150X—Heat A-1 with 0.05 per cent aluminum addition showing increased number of nodules. Fig. 12D (Lower Right), 150X—Heat A-1 with 0.18 per cent aluminum addition. Nodules and considerable mottling are present.

condition in the same heat of iron as that of Fig. 12A, 12B. and 12C, but with the large addition of 0.18 per cent aluminum. The foregoing graphitization phenomena thus seems to reproduce in Foundry A melted irons the principles established under laboratory melting conditions. However, exactly the same results were not obtained in Foundries B and C as is pointed out below.

The effects of deoxidation on graphite nucleation and distribution described above require some clarification and qualification. These effects were most reproducible when the iron treated with deoxidizers was initially in a sufficiently oxidized condition as was the case with the irons from Foundry A. This condition is described most simply by references to the sprue test. If greater than 0.10 per cent aluminum, preferably about 0.12 to 0.15 per cent was required to produce mottle in a test sprue, then the foregoing principles of graphitization seemed to function de-

pendably. However, when the amount of aluminum required to produce mottle in the test sprue is only 0.02 to 0.10 per cent then the initial effect of a decrease in number of nodules with the smallest aluminum addition appears much less or is even lost entirely, the effect becoming less evident as the smaller aluminum additions for mottling are required. Heats from Foundry B evidenced little, if any, decrease in nodule number with small aluminum additions as compared with Foundry A irons, while those from Foundry C showed no decrease at all. However, in each case the flake-like particles disappeared from the structure. The nodule decrease due to small aluminum additions thus appears linked with some deoxidation phenomena that is absent when the initial oxygen content of the heat is low prior to the treatment. The diminished number or absence of free silicate inclusions may ultimately provide explanation.

A further qualification on the graphite nucleation

principles lies in the section size being annealed. This was especially noteworthy in irons which were nearly completely deoxidized. Originally the author¹ reported a very fine flake-like graphite distribution for completely deoxidized metal. This degree of graphite nucleation was never obtained, however, in the tensile bar section sizes studied in this investigation. The disagreement, however, was resolved when size of sample was considered. The initial work was done with metallographic samples considerably smaller than test bar size. As is well known, the graphite nucleates much more readily in the smaller samples. When small pieces were cut from the white iron bars containing Type II inclusions and subsequently annealed, a very fine graphite dispersion was found to exist.

While the points considered above are significant in reproducing the graphitization principles suggested they obviously are not the only ones. Rising or falling temperatures in the melting furnace, in the ladle, additions to furnace or ladle, time at temperature adjustments in air and fuel are also of considerable importance. Of course, the data in this work pertain to metal tapped direct from the furnace and not to that held in a large ladle. Obviously other variables

could be listed.

Summarizing, the effects of deoxidation on graphitization result in an initial decrease in the number of nodules and then an increase as the percentage of aluminum increases providing the initial oxygen content of the iron is sufficiently high.

Residual Phase

Metallographic study of the fully malleabilized deoxidized test bars revealed the presence of a new phase in the grain structure. In addition to the ferrite and graphite areas normally present, a hard, white phase was found to occur. This phase was observed to increase in amount as the aluminum addition increased above that necessary to produce Type II inclusions. The first appearance of this phase in the structure slightly precedes the presence of a definite percentage of 0.01 per cent or more of residual aluminum in the chemical analysis of the metal, but from the metallographic and analytical data available probably does not occur until a definite though minute percentage of alloying aluminum is present.

The phase is illustrated at 1000X in Fig. 13A and 13B. The samples in the figure were obtained from Foundry B, Heat 1. This phase may also be observed in the low magnification photomicrographs, Fig. 12D and 16. Two characteristic distributions of the phase seemed to exist. It appeared mainly as rounded, white islands similar to carbides but which do not break down even with the most extended annealing at 1700 F, or lower temperatures. It is dark etching in alkaline picrate as is the normal carbide. A second distribution of this phase seemed to occur as a grain boundary precipitate as revealed in Fig. 13A and 13B. This grain boundry material was prevalent in all aluminum-treated samples with Type II and III inclusions and is believed to be primarily responsible for the loss in ductility and tensile properties which occurs with the larger aluminum additions. The grain boundary phase was not observed in samples where less aluminum was added than that required to produce Type II inclusions but it may have been present in amounts not metallographically detectable. It was not possible in all cases to identify positively the grain boundary material as being exactly the same as the larger, rounded areas. In some cases a part of the network seemed to consist of aluminum nitrides and it may be that both nitrides and the metallic phase may coexist in the boundaries.

The relationship of the new phase to the amount of aluminum addition is summarized in Table 2. Additional information was provided in Table 2 to show the relationships of the phase to per cent residual aluminum, inclusions type, and mottling tendency. It is seen from Table 2 that for the most part the phase occurs in definite, readily discernible amounts

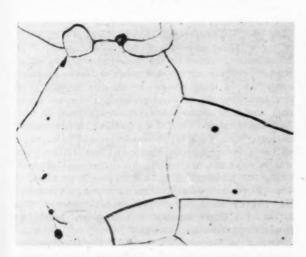
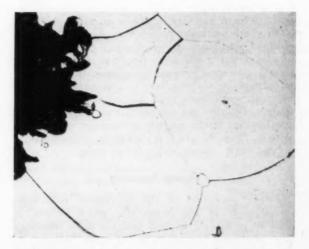


Fig. 13A (Left), nital etch, 1000X—Heat B-1 with 0.18 per cent abuminum added. Rounded white islands are the hard residual phase which are dark etching with



1 with 0.18 sodium picrate. Fig. 13B (Right)—Same material as islands are Fig. 14A. Small islands of residual phase close to graphite areas. Graîn boundary material is present both in Fig. 13A and 13B.

as soon as the aluminum additions sufficient to produce Type II inclusions are present. Thus, the presence of a definite amount of residual aluminum in the analysis, tendency to mottle, the appearance of the additional phase, and marked subcritical graphitization tendencies are concurrent with Type II inclusions, i.e., aluminum additions sufficient in magnitude to deoxidize the melt to the extent necessary to pro-

duce Type II inclusions.

The additional phase discussed in the foregoing section has been reported previously by a number of investigations. Yarne* demonstrated the presence of this phase in aluminum-treated fully-malleabilized standard irons in a discussion of the author's initial paper1 on deoxidation of white cast irons. The most complete description of this phase is reported in an investigation of the ternary iron-carbon-aluminum alloy system by Lohberg and Schmidt.4 They name it the K-phase and on the basis of X-ray data, chemical data, and metallographic and equilibrium diagram studies describe it as a double carbide of aluminum and iron. The latter investigators state that aluminum in iron-carbon alloys differs from silicon in that the former is initially a carbide former whereas the latter forms silicides with iron. They also show that the K-phase increases in amount with increase in aluminum content until at about 12 per cent aluminum no graphite is present in annealed iron-carbon-aluminum alloys where the per cent carbon is 2 to 2.5 per cent. According to their work, aluminum exhibits the characteristics of other strong carbide-forming elements such as titanium, in that it considerably raises the critical temperature range, restricts the gamma field, and increases the per cent carbon soluble in the austenite and the per cent carbon in the eutectoid. It is also thought provoking to note that aluminum and titanium, as pointed out in a later section, are similar to boron in this effect of producing a phase that does not graphitize.

While it is not the purpose of this investigation to determine the alloying effects of aluminum, it appears that a critical study of this element is necessary in order to learn its exact functions in cast irons. The point at this particular time is that any simple classification of aluminum as a graphitizing element similar to silicon but much more powerful, 6 to 10 times according to some authors, is not compatible with the actual experimental results so far published. In malleable iron, small amounts of residual aluminum in the analysis, i.e., that not combined with oxygen during deoxidation, results in the appearance of a third metallic phase, K, which is present in addition to the normal ferrite matrix and graphite nodules.

Physical Properties

Tensile properties of the deoxidized irons were investigated. In any comparison which follows, it must be recognized initially that the three foundries were producing basically different irons. Foundry A, for example, was producing Grade A malleable iron with practically negligible deviations from specifica-

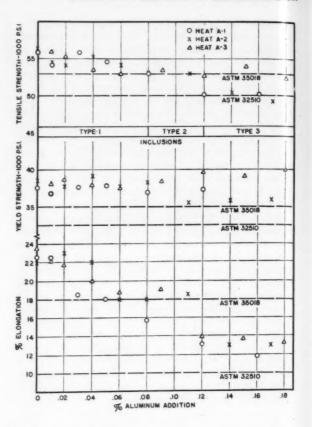


Fig. 14—Graph showing effect of deoxidizing additions of aluminum on physical properties of irons from Foundry A. Annealing cycle, 196 hr.

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tions while Foundry C was primarily interested in a stove plate quality iron.

Data from Foundry A were most consistent and it is shown graphically in Fig. 14, each point being the average of 3 or 4 bars. From Fig. 14 it is seen that small additions, up to 0.06 per cent had relatively little effect on properties. However, as the magnitude of the addition approached that which caused Type II inclusions, scatter of the data and a falling off of the properties occurred. A number of factors probably contribute to these effects. Increase in nodule number, mottling, the residual phase in the grain boundary and unfavorable inclusion distribution, all may be listed as causes. Obviously, mottling, which usually occurred with Type III inclusions, would be expected to lower the properties drastically, but its effect was not so great as anticipated. An illustration of weakening due to metallographic structure is provided in Fig. 15. A string of inclusions may be seen extending from upper left to lower right across the picture. The chain or stringer type of inclusions are known to lower elongation and strength.

Data from Foundry B were plotted in the same way as that shown in Fig. 14. The same general trends were observed but with the effect of having the plot of Fig. 14 shifted to the left about 0.04 per cent. However, the properties initially were slightly below Grade A in the particular three heats studied. The drop off

^{*} J. Yarne, Metallurgist, The Chain Belt Co., Milwaukee, Wis.

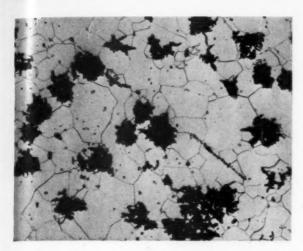


Fig. 15—Heat A-1, 0.12 per cent aluminum addition. Structure in malleabilized tensile test bar showing a string of inclusions which appear to extend from upper left to lower right in a straight line. Mag.—150X.

in physical properties occurring at the right in Fig. 14 began to occur immediately with small aluminum additions. This may be due to the fact that less aluminum was required to produce Type II inclusions in these irons than in those of Foundry A, i.e., the effect of moving the plot of Fig. 14 to the left. It would appear from this consideration that the irons in which a lesser deoxidizing effect occurs are those in which the properties begin to fall off more rapidly and in which the properties are not as good initially. The fact that the graphite nodule number does not at first decrease before it begins to increase when less oxidized irons are treated with aluminum may be the reason the properties of the latter irons are effected adversely at lower aluminum additions.

TABLE 4-EFFECT OF ANNEALING CYCLE

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Heat 2, Foundry B; 2.44 C, 0.72 Si, 0.33 Mn, 0.130 P, 0.110 S

| Property | T.5 | S., psi | Y.F | ., psi | Elon | g. % |
|-----------------|------------|--------------|-----------|--------|-----------|-------|
| Cycle | Long B* | Short C** | Long B | Short | Long B | Short |
| Heat B-2 | | | 1 | - | | - |
| 000 Al | 53,900 | 50,000 | 38,650 | 36,300 | 15.0 | 17.0 |
| 0.01 | 52,950 | 51,000 | 35,700 | 36,900 | 13.75 | 17.0 |
| 0.03 | 53,600 | 51,100 | 36,400 | 37,200 | 13.00 | 17.0 |
| 0.05 | 53,800 | 47,500 | 35,950 | 36,000 | 13.75 | 12.0 |
| 0.07 | 51,500 | 46,600 | 35,400 | 36,100 | .11.25 | 11.5 |
| 0.09 | 48,550 | 45,800 | 34,150 | 34,700 | 11.50 | 11.5 |
| 0.13 | 46,100 | 43,000 | 33,000 | 33,100 | 8.50 | 9.0 |
| | 150 Hr | ** 5 | 4 Hr | | | |

Tensile bars from Foundry C did not display the same property trends described previously, Fig. 14, and this difference in behavior was found to be an influence of the annealing cycle.

Effect of the annealing cycle was revealed by results obtained when bars from Foundries A and B were annealed at Foundry C and bars from Foundry C were annealed at Foundry B. Typical results for metal from Foundry B, Heat 2, annealed at Foundries C and B are given in Table 4. The trend of properties ob-

tained with the short cycle anneal used at Foundry C is different from that obtained with the longer cycle. An initial improvement occurred followed by a dropoff. The difference in effect, obtained solely from the annealing cycle differences, may account for the pronounced disagreement in the literature on the effect of aluminum additions on physical properties. Foundry C was consistent in this respect in that when additions of 0.01 to 0.04 per cent aluminum were added to the metal, about a 10 per cent improvement in properties was obtained in their own metal with their own annealing cycle. However, when the same metal from Foundry C was annealed at Foundry B poor physical properties were obtained. Thus, it appears that the influence of deoxidizing treatments are dependent not only on the magnitude of the addition and the initial oxygen in the melt but on the annealing cycle as well.

Subcritical Graphitization

The author has reported previously on the subcritical graphitizing tendencies of deoxidized irons. When aluminum additions are sufficient to produce Type II inclusions, pronounced subcritical graphitization can occur; at 1200 F, for example. This was also observed to be true in the foundry irons presently investigated. The phenomenon is merely reported in this paper as an observation of the effect of deoxidation with aluminum to Type II inclusions since this subject is a lengthy one in itself.

Effects of Deoxidation with Titanium

In the case of Heat A3, the effects of titanium additions were investigated. Titanium was added as powdered titanium metal to avoid the effects of the small amount of aluminum usually present in ferrotitanium. As with aluminum additions, similar principles seemed to be functioning in the case of deoxidation with titanium. Inclusion changes occurred although they were somewhat different than those for aluminum. Small titanium additions produced relatively little change except that the sprawly nature of the Type I sulphides disappeared and they become blocky in shape. Titanium additions of 0.12 to 0.24 per cent produced a film type of sulphide as shown in Fig. 16. These are referred to as Type II-Ti for lack of a better designation. However, the fine dot-like Type II dispersion reported for aluminum additions was never obtained. The highest addition, 0.27 per cent, revealed an unusual sprawly eutectic pattern as shown in Fig. 17A and 17B. The sulphides in this case were pale yellow or tan as compared with the usual dove gray color. These have been reported in cast steel and have been referred to as containing a large proportion of titanium sulphide.2 Of course, as before, Type I inclusions exist in the untreated iron. Type III inclusions were not obtained in these particular samples.

Test sprues were cast for each titanium addition. The maximum addition, 0.27 per cent, produced a mottled sprue. The mottle, however, occurred in fine specks, smaller than is usually the case. Metallographic examination proved this mottle to be a flake graphite aggregate, as expected.

The influence of titanium additions on graphitiza-

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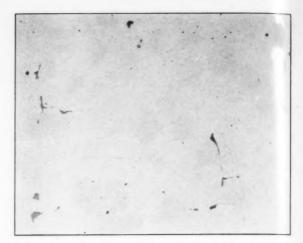


Fig. 16-Heat A-3 with 0.12 per cent Ti. Type II-Ti, sulphides, no silicates. Mag.-750X.

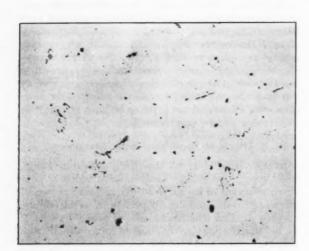


Fig. 17A (Left), 250X-Type II inclusions in Heat A-3 with 0.27 per cent titanium additions. Fig. 17B



(Right), 750X-Type II eutectic inclusions and some duplex inclusions.

tion was similar to that of aluminum in this heat. There was at first a decrease in the number of nodules and then a slight increase with increasing percentage of titanium addition. However, the increase in nodule number did not occur to nearly the same extent as with aluminum. Flake-like graphite and sprawling nodules occurred with the highest addition.

The residual phase described previously for aluminum also made its appearance in the titanium-treated iron. The hard particles were extensively in evidence in the samples with highest titanium addition as was also the grain boundary material. The residual phase was so much the same as that appearing from aluminum treatment that it was not possible to distinguish between them metallographically except that a much greater amount of hard phase was present in the titanium-treated iron. Photomicrographs would reveal the same appearance as Fig. 14A and 14B. They are therefore not reproduced here.

The physical properties obtained from the titaniumtreated iron are given in Table 5.

TABLE 5-PHYSICAL PROPERTIES OF TITANIUM-TREATED

| Titanium addition, % | Tensile Strength, psi | Yield Point, psi | Elongation, | |
|-------------------------|--------------------------|---------------------|-------------|--|
| 0.015 | 55,100 | 39,600 | 20.6 | |
| 0.030 | 55,100 | 37,900 | 20.5 | |
| 0.090 | 55,000 | 36,650 | 21.0 | |
| 0.120 | 55,500 | 36,200 | 21.25 | |
| 0.180 | 56,100 | 37,350 | 21.70 | |
| 0.240 | 56,000 | 38,800 | 21.50 | |
| 0.270 | 53,000 | 37,550 | 14.5 | |

Since the effect on graphite nodules and inclusions was much milder in the titanium-treated irons, the effect on physical properties is less pronounced. This may be due to the fact that titanium is classified as a less efficient deoxidizer than aluminum.² However, consistent with the effects caused by deoxidation with aluminum, a titanium addition sufficient to cause mottle in the test sprue, Type II inclusions, and the appearance of a residual phase was one which pro-

duced a marked drop-off in physical properties.

The parallelism in effects of titanium and aluminum treatments substantiate the deoxidation mechanism as being responsible for the changes noted. Titanium is known as a powerful carbide forming element in its usual role in ferrous alloys. The present tests reveal its graphitizing effects through deoxidation up to the point where actual alloying results in carbide stabilization and the appearance of the hard residual phase.

Discussion

Oxidation of the melt is a definite phase of the melting practice of white irons used for malleable iron castings. As is frequently the case in other ferrous foundry melting operations, an oxidizing period is utilized to obtain the chemical composition, and other characteristics desired when the metal is cast and subsequently annealed. For example, carbon and silicon in the metal are oxidized down to the desired limit by means of a gentle boil during the melting period. As a result of this oxidizing phase of the melting practice, a certain percentage of oxygen is introduced into the bath. The oxygen which remains in small residual percentages when the metal is cast and solidifies may have a considerable influence on the characteristics of the iron. Although no oxygen analyses have been made of the metal as extracted from the bath, an oxygen content exceeding that predicted by thermodynamic data could be expected if data obtained from steel open-hearths is referred to.5 It is the latter form of oxygen in the metal that reacts with deoxidizing additions and produces some of the effects described in this paper.

In considering the data obtained from the deoxidation experiments, several pertinent observations arise. It appears that the percentage of residual oxygen in the melt may vary considerably in different heats in the same foundry, and also from foundry to foundry. The previous statement, of course, is founded on the basis of considering the variation in residual oxygen in the different heats to be measured by the percentage of aluminum required to produce the deoxidation effects described in the foregoing sections of the paper. This assumption is lacking in rigorous quantitative proof, i.e., chemical analysis for oxygen, but appears valid in the light of deoxidation knowledge existing for cast steels.2 On this basis, the metal as it came from the air furnace required from 0.02 to 0.12 per cent aluminum for complete deoxidation which is equivalent to about 0.02 to 0.11 per cent oxygen in the melt. Some observations on the reasons for this variation in oxygen content may be made. It is usual in other melting procedures involving an oxidizing period, to practice a definite deoxidizing step prior to tapping. However, in malleable iron melting this has been the exception rather than the general practice. In cast irons, a deoxidizing step in the melting period has been considered unnecessary because it was thought the high carbon and silicon percentages present automatically insured an extremely low residual oxygen content. The validity of the latter assumption is questionable. The ability of these two elements to decrease the oxygen content of the melt to the lowest value of which they are capable depends on the attainment of chemical equilibrium in their deoxidation reactions. However, nonequilibrium rather than equilibrium prevails in most cast iron melting. A gentle boil, for example, persists in the furnace while white iron is being tapped. Of the two elements, silicon has been shown able to decrease the oxygen to a lower percentage than carbon if equilibrium is attained. Yet, it has been observed in the latter stages of melting in one foundry that silicon is not oxidized by the boil and hence is not functioning as a deoxidizer. Data for the preliminary and final analysis of a number of heats from Foundry A illustrate this effect as shown in Table 6.

TABLE 6

| Preliminary | (1/2 hr. bef | ore tap) | At tap (no silic | on additions) |
|-------------|--------------|----------|------------------|---------------|
| Heat No. | C, % | Si, % | C, % | Si, % |
| 29 | 2.61 | 1.09 | 2.36 | 1.14 |
| 32 | 2.66 | 1.06 | 2.33 | 1.13 |
| 33 | 2.64 | 1.09 | 2.32 | 1.17 |
| 34 | 2.54 | 1.08 | 2.41 | 1.13 |
| 62 | 2.47 | 1.06 | 2.24 | 1.17 |
| 63 | 2.42 | 1.05 | 2.39 | 1.09 |

Foundry A followed the practice of melting down a charge of about 1.30 to 1.35 per cent silicon, higher carbon than desired and oxidizing down to desired analysis. Carbon was added as carbon raiser or petroleum coke to keep carbons up if the preliminary analysis showed a low carbon percentage. Ferrosilicon was not added unless a low silicon was encountered on the previous heat. The heats shown in Table 6 were ones in which no silicon was added. The increase in silicon thus would appear to be due to reduction of silica or silicates during the late stage of the melting process since oxidation of silicon from about 1.30 per cent down to 1.05 to 1.09 per cent occurs during the early stage of the heat. The phenomenon of "silica reduction" is well known in ferrous melting and usually occurs at high temperatures and results in conditions where silicon is not acting to decrease oxygen in the melt. In this case the preliminary analysis sample is taken with the metal at about 2700 F while the metal at tap is about 2840 to 2880 F. It was noted on several occasions in each of the three foundries that sprue test bars were mottled at the time of the preliminary but white at tap. This occurred, for example, with Heat 63, Table 6, and it provides a typical case of a minor change in carbon and silicon accompanying the major change from mottled to white iron. The effect of the higher temperature favoring higher oxygen residual in the iron and thus causing this change to occur should not be overlooked. The effect of carbon-drop and raised silicon percentage in molten cast irons under oxidizing conditions at temperatures of 2800 to 2900 F is not peculiar to foundry operations since it has been verified in the laboratory for induction melting conditions. In the light of the foregoing comments, it appears extremely dangerous to assume that the equilibrium conditions prevail where carbon and silicon may function to decrease the oxygen in the melt to a few thousandths of a per cent under normal melting conditions. Further, neither carbon nor silicon under their most effective conditions could lower the oxygen to the extent reached by more powerful deoxidizers. It also appears that melting conditions

that favor "silica reduction" and silicon pick-up would be those that favor a higher oxygen residual in the iron.

Some differences in melting practice were noted that appear to be related to the results. It may be noted that about 0.02 to 0.06 per cent aluminum in Foundry C, 0.04 to 0.08 per cent aluminum in Foundry B, and 0.08 to 0.12 per cent aluminum in Foundry A were required to produce Type II inclusions or virtually complete deoxidation. Foundry C thus produced irons with a minimum residual of oxygen while Foundry A produced irons with a maximum of residual oxygen of the three foundries studied. The residual oxygen seemed to be related to the iron-coal ratio. Foundry C employed the lowest iron-coal ratio, 2 to 1, Foundry B was next with 2.86, and Foundry A had the highest ratio of 3.16 to 1. Foundry B consistently followed the practice of making ferrosilicon additions to raise silicon about 20 min before tap. The latter practice may account for irons from this foundry requiring less aluminum for deoxidation than those from Foundry A since the ferrosilicon itself would produce some deoxidizing effects.

In the foregoing paragraphs, several conditions have been pointed out as being sources of possible variations in residual oxygen in the iron; namely, "silica reduction," iron-coal ratio, and furnace additions. Others may be named. Oxygen in the charge materials, slag composition, fuel combustion adjustments, water in the blast or fuel, and so on, to list a few. Whatever the sources, the data indicate that the oxidizing phase of melting produces a variation in residual oxygen if deoxidation tests are accepted as satisfactory measurement of such variations.

While the deoxidation requirements are markedly higher in the heats from Foundry A, it should not be considered that these irons nor any of the others described represent an overly "oxidized" condition. Rather they are oxygen variations which occur in irons that anneal satisfactorily under the prevailing conditions but possibly with undesirable fluctuations in properties. Since graphite nucleation, nodule number and annealing time are affected by the oxygen in the metal, it can be seen that fluctuations in the residual oxygen may cause fluctuations in the properties after annealing. From the limited data available to the author, it appears that an iron of but enough oxygen content to require a minimum of 0.12 per cent aluminum to produce mottle in the sprue test will produce consistently better physical properties than those requiring less aluminum for mottling in the sprue test; the one requiring higher aluminum being higher in oxygen content. Further investigation of the sprue test as a means of evaluating the iron, if carried out on a statistical basis, might prove it to be a valuable method of testing or control of the variables introduced by oxidation during the melting period.

Conclusions

A review of the data presented herein permits some generalizations which are tabulated below:

1. Three basic types of inclusions may occur in white cast irons. These are Type I or random sul-

phides and silicates, Type II or eutectic sulphides, and Type III random sulphides, some duplex sulphides, and alumina; the latter two types being the result of a deoxidizing addition as in the well known case of cast steels.

2. A sprue test has been described which is able to evaluate separate heats of iron on the basis of their tendency to mottle. Since the sprue test employs a deoxidizer, it appears able to measure the effect of the oxidation phase of the melting process in introducing oxygen into the metal. Mottling in a test sprue may be produced both with aluminum and titanium.

3. Graphite nucleation is affected by deoxidation with aluminum and titanium. Small additions result in fewer, larger nodules but only if the metal is initially sufficiently oxidized. In the same irons, larger additions, 0.05 per cent or greater, result in increased nucleation over the untreated material. When an iron is not initially high enough in residual oxygen to cause the foregoing nucleation effects to occur, the immediate effect of deoxidizing additions is to increase the number of nodules.

4. Additions of aluminum and titanium which are sufficient to produce virtually complete deoxidation and an alloying residual percentage of the element, cause a new metallic phase to appear in the structure of fully malleablized irons. This phase is considered to be a complex carbide.

5. Small additions of aluminum or titanium influence physical properties in a manner which depends on the residual oxygen content in the melt.

6. Deoxidizing additions of sufficient magnitude to produce Type II inclusions, the residual phase, pronounced increase in nucleation, or mottling, result in much lowered physical properties.

7. Different melting practices may produce considerable variation in the amount of residual oxygen in the iron as judged by the sprue test, inclusions, physi-

cal properties and residual phase.

8. Physical properties of malleablized test bars appeared to be better when higher percentages of aluminum were required to produce mottle in the sprue tests; that is, within the limits of this investigation.

9. The physical properties of an iron are revealed to be dependent on the annealing cycle. Malleabilized irons may show improvement or deterioration in properties with deoxidizing additions depending on the cycle employed.

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10. Small percentages of aluminum and titanium added to white cast irons function initially as deoxidizers while amounts sufficient to leave a residual alloying percentage form complex carbides in the malleabilized iron. The former behavior promotes graphitization and ultimately mottling.

Acknowledgments

The author wishes to express his sincere thanks to many who provided him with the opportunity and assistance in carrying out this investigation; particularly, to the following: Mr. J. Goodwin, Foundry Superintendent, and Mr. J. Rhodes, Plant Manager of the Monarch Malleable Iron Range Co., Beaver Dam, Wis.; Mr. J. Scales, Chief Metallurgist, Mr. John Yarne, Metallurgist, Mr. W. B. Sobers, Metallurgical

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Chemist, and Mr. J. Kropka, Foundry Superintendent of the Chain Belt Company, Milwaukee, Wis.; Mr. W. V. Osborne, President and Mr. R. V. Osborne, Vice-President, Mr. L. Osborne, Secretary, Mr. M. Harder, Foundry Superintendent, and Mr. H. McCabe, Metal-Jurgical and Chemical Laboratory, all of the Lakeside Malleable Castings Company, Racine, Wis. Appreciation is extended to the foregoing for providing foundry facilities and technical assistance in obtaining the data on which this paper is based. The author also wishes to thank his associates, Prof. P. C. Rosenthal, Research Metallurgist, L. F. Porter, and Prof. G. I. Barker, Chairman of the Department of Mining and Metallurgy of the University of Wisconsin for their suggestions and encouragement.

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DISCUSSION

Chairman: H. C. STONE, Belle City Malleable Iron Co., Racine, Wis.

Co-Chairman: E. Roby, Jr., Peoria Malleable Casting Co., Peoria, Ill.

MILTON TILLEY (Written Discussion)1: The author deserves a vote of thanks for a job well done on a hitherto neglected subject of prime importance to the malleable industry. Mr. Heine has submitted some preliminary work on a subject which I hope will receive considerably more attention.

Although I am not prepared to subscribe to all the conclusions drawn on this preliminary work, I can say that the results

parallel closely the work done in our shop.

Being primarily a production man rather than one of research my interest has been in using to advantage the information

immediately in production work.

Toward this objective I have found that in our cupola melting practice that when the oxidation rate of silicon exceeds about 25 per cent, increasing rates lower the nodule count and therefore the annealability at a very alarming rate, somewhat in the order of a decrease of 1000 nodules per cubic millimeter for each increase of 10 per cent loss in silicon.

Obviously, this is information which can be used to advantage in melting operations. Also, it has been demonstrated that by the use of deoxidizing reagents (in practice, ferro silicon) in the proper ratio, overly oxidized iron may have its graphitizing rate restored to the point where it will anneal satisfactorily in

the established cycle.

I am not prepared to say that the nodule number has been fully restored since the evidence tends to contradict. This point, of course, poses the question of the role of silicon on rate of graphitization aside from the number of nodules, or the effect of the degree of oxidation and deoxidation.

The subject has been so meagerly covered in the past that it is hoped that Mr. Heine and others will spend much more time

on it in the future to the industry's advantage.

J. E. REHDER (Written Discussion): 2 In this paper, Prof. Heine is continuing his very good work on the study of nonmetallic inclusions in malleable iron as affected by ladle additions to the molten iron. Considerable data are presented which are a useful addition to our sketchy knowledge of nonmetallic inclusions, particularly of aluminum type, in malleable iron.

However, there would seem to be some differences of opinion on the interpretation to be placed on the experimental results. To assume that the effects of adding a deoxidizer to white iron are due to deoxidation, would seem to be begging the question, and the absence of analyses for oxygen content of the samples studied in the present paper is unfortunate. It is hoped that

Prof. Heine may have these done.

The term "deoxidation" may be under some misinterpretation, since to the steel maker it usually means decreasing the dissolved oxygen or FeO content, which can be high in low-carbon steels. The fact remains that carbon and silicon are effective deoxidizers, especially when present in such quantities as in malleable iron or cast iron, and the amount of dissolved oxygen or FeO must be at a low level. However, oxygen can be present in a molten white iron as a dispersion of silica or silicates, and the addition of a deoxidizer more powerful than silicon results simply in the transfer of oxygen from silica to, for example alumina, with the release of a proportional amount of free silicon. Since silicon is a graphitizer while silica is inert, some increase in graphitization would be expected.

Inspection of Fig. 14, showing mechanical properties of malleable irons treated with increasing amounts of aluminum, indicates that from the practical viewpoint aluminum would be a good thing to keep out of the iron. There is a distinct trend downward in tensile strength as soon as aluminum is added, and the effects on elongation are severe. No advantage to mechanical properties by addition of aluminum is shown. Production of a higher nodule count, or greater numbers of smaller nodules, is not in itself always desirable, since although annealability is thereby usually increased, mechanical properties deteriorate. Use of boron for this purpose has been in some vogue, but mechanical properties are poor when the nodule

count is greatly increased.

Not mentioned in the paper were one or two points on which Prof. Heine's answer would be appreciated. When aluminum is added to malleable iron or cast iron, a skin of presumably aluminum oxide forms on the molten iron and makes it very sluggish to pour. In the tests described, were any light sectioned castings poured, and if so was there any difficulty due to misruns, slag inclusions, or pin-hole porosity? Was any effect noted on tendency to hot cracks?

With respect to additions of titanium, results reported in Table 9 show that mechanical properties did not deteriorate until large amounts of titanium were added. This is different from the case with aluminum additions, and suggests that the effects of aluminum and of titanium are not parallel.

It is believed that Prof. Heine's suggestion that the standard white iron fracture sprue be used as an indicator of state of oxidation, should be approached with a great deal of caution, since there are many factors that affect mottling tendency. It is well known that superheat alone will clear up a mottled

The observation of a gentle boil in the air furnace during tap-out has been noted by Prof. Heine as a proof of nonequilibrium conditions in the furnace. This is correct in statement, but possibly not in interpretation. It is well known that as the iron becomes hotter, especially over about 2830 to 2850 F, the carbon in the iron reacts with the silica in the furnace bottom to form silicon, which dissolves in the iron, and carbon monoxide gas, which rises as bubbles to the surface of the iron. Thus the self-deoxidizing power of the molten white iron bath is sufficient to actually start deoxidizing, or reducing, the silica bottom. The higher the temperature of the iron, the more rapidly does this reaction proceed.

With respect to mottling tendency versus the source of the iron, the differences noted between the permissible aluminum addition for Foundry A and Foundry C must also take into account the fact that Foundry C iron was 0.35 per cent higher in carbon content than Foundry A iron, which in itself would in-

crease mottling tendency

To summarize, Prof. Heine's work is interesting and valuable,

¹ National Malleable & Steel Castings Co., Cleveland

² Foundry Engineer, Bureau of Mines, Ontario, Canada.

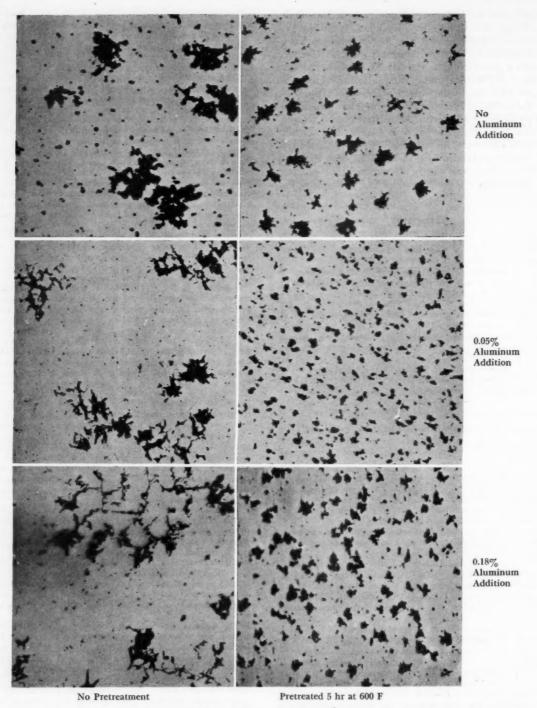


Fig. A—Comparison of graphite pattern in standard and aluminum-treated malleable iron after completion of first stage graphitization. Unetched. ×100.

but it is believed that more fundamental work should be done and more variables be controlled before interpretations of results can be on a sound basis.

J. L. YARNE (Written Discussion): Professor Heine's work is a valuable addition to the metallurgy of malleable iron. It is important to remember that these tests were made with normal foundry irons, melted and poured under production conditions. There is still much work to be done before the effects of all of

the variables may be understood. In conjunction with Prof. Heine, we have studied some of the factors which appear important. I wish to point out a few of the things that we found.

First, we found that pretreatment has a profound effect on the graphite pattern. This is illustrated in Fig. A, where the effect of pretreatment of iron with varying aluminum content is demonstrated.

It may be noted that the irons having aluminum additions and no pretreatment have a lacy graphite pattern that almost has the appearance of primary graphite. Pretreatment of 5 hr at 600 F

⁸ Research Metallurgist, Chain Belt Co., Milwaukee, Wis.

before annealing (first stage at 1700 F), eliminated this objectionable feature. It may be interesting to note that in this particular iron, pretreatment also was effective in increasing the nodule number of the untreated iron. However, this does not always occur. Some irons do not respond to pretreatment. Perhaps the degree of oxidation or the presence of critical amounts of hydrogen inherited from the melting conditions may account for this phenomenon. The iron illustrated had the following analysis:

T.C. Si Mn S P Cu Cr Ni Mo 2.47 0.98 0.33 0.120 0.130 0.08 0.01 Nil Nil

Another important feature is the behavior of the physical properties with increasing aluminum contents. This is demonstrated graphically in Fig. B.

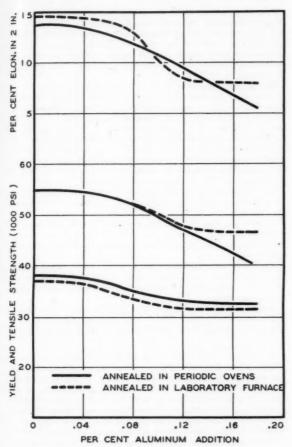


Fig. B-Physical properties of malleable iron as affected by additions of aluminum.

Duplicate sets of test bars were annealed in the periodic ovens under commercial conditions and also in the laboratory. Those bars annealed in the laboratory were all pretreated for 5 hr at 600 F. The annealing cycle was adjusted for each condition, that is, the bars having aluminum additions were annealed under optimum conditions as determined previously from other test bars. It may be seen that when the aluminum addition exceeds 0.04 per cent, the physical properties begin to decrease. This was true for three different heats which by Prof. Heine's sprue test had shown different degrees of deoxidation. It will also be observed that in those bars annealed in the periodic ovens the tensile strength and ductility steadily decreased with increasing aluminum content, but in those bars annealed in the laboratory the physical properties began to level off at approximately 0.12 per cent aluminum. I think this difference can be explained by the pretreatment which gives a better graphite pattern. As the aluminum increases, the graphite pattern tends to become flaky unless counteracted by pretreatment.

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It is hoped that this brief discussion may stimulate further investigation and point out some of the variables which require close attention.

H. A. SCHWARTZ (Written Discussion): ¹ Prompted largely by Prof. Heine's statements that mottling is largely a consequence of the absence of oxygen, George Wolff has investigated, in this laboratory, the effects of vacuum melting on the fracture of remelted white cast iron. Except for Prof. Heine's experiments with hydrogen ("Some Effects of Deoxidation Treatments on Graphitization of White Cast Iron," R. W. Heine, Transactions, A.F.S., vol. 57, page 315, 1949) his observations are just as much in agreement with the conclusions that graphitization is accelerated by the formation of nuclei of Al₂O₃ and the like as by the elimination of oxygen as iron oxide. This mechanism was previously suggested by others ("Factors Controlling Graphitization of Carbon Steels at Subcritical Temperatures," Transactions, ASM, vol. 35, page 485, 1945—by Austin and Fetzer).

The elimination of CO formed from oxygen and carbon in the metal was thought to have the advantage of removing oxygen without addition of new components, or, presumably phases.

In the first series of experiments various white cast irons were remelted in zircofrax crucibles in the Arsem furnace and held liquid for ½ hr. The furnace atmosphere is largely CO at a pressure of say 1 or 2 mm of mercury. It was expected that at this low CO pressure the reaction of the carbon in the iron with any oxides would produce a fairly complete elimination of any oxygen. The specimens required about 20 min to cool to a black heat in the vacuum.

In the following table the composition of the charge, and, when determined, of the resulting metal, are summarized.

All the fractures were gray (on finger-sized pins).

TABLE A-ZIRCOFRAX MELTS

| | | | | | ion, Per C | Cent | | | |
|-----|------|--------|------|-------|-------------------------|------|------|--------|-------|
| | | Charge | | | | | Melt | | |
| No. | C | Si | Mn | S | C | Si | Mn | S | P |
| 1. | 2.45 | 1.26 | 0.69 | _ | _ | _ | _ | _ | _ |
| 2. | 22 | | 22 | - | _ | _ | - | _ | _ |
| 3. | 2.48 | 1.25 | 0.69 | 0.163 | 2.46 | 1.30 | 0.66 | _ | 0.195 |
| 4. | 2.66 | 1.10 | 0.71 | 0.167 | 2.61 | 1.15 | 0.64 | 0.159 | _ |
| 5. | 3.10 | 1.21 | 0.42 | _ | nime. | _ | | _ | - |
| 6. | 2.72 | 0.95 | 0.35 | _ | 2.63 | 1.01 | 0.35 | _ | - |
| 7. | 2.48 | 1.10 | 0.54 | _ | 2.42 | 1.13 | 0.52 | _ | - |
| 8. | 2.36 | 1.02 | 0.49 | 0.163 | 2.35 | 1.12 | 0.46 | 0.154 | - |
| 9. | 2.45 | 1.06 | 0.54 | 0.163 | 2.38 | 1.18 | 0.51 | _ | _* |
| | | | | | itrogen as apidly as | | | rent w | as cu |

In all cases there was some reduction of carbon, perhaps due to the deoxidizing reaction. There is always some volatilization of manganese in vacuo. There is always some pickup of SiO₂ which, together with lively melt, caused us to suspect a reaction with the crucible, although no marked attack was visible. Two samples (the sixth and last of the table) were analyzed for zirconium, giving concentrations near 0.006 per cent.

We are left with the conclusion that either slow freezing, less than 0.01 per cent Zr or vacuum melting, will produce gray fractures in compositions where this was by no means suspected. We doubted whether the slow cooling is the explanation but the effect of Zr is not too surprising in view of much earlier work.

A number of melts were then made in graphite crucibles lined with magnesia, as described below:

TABLE 2

| | | | | Compo | sition, | Per C | Cent | | |
|-----|------|------|-------|-------|---------|-------|------|-------|------------------------|
| | | Cl | narge | | |] | Melt | | Fracture |
| No. | C | Si | Mn | S | C | Si | Mn | S | |
| 7. | 2.72 | 0.95 | 0.35 | _ | 2.64 | 0.95 | 0.34 | _ | Gray |
| 8. | 2.36 | 1.02 | 0.49 | 0.163 | 2.32 | 1.03 | 0.42 | 0.106 | Microscopio Mottles |
| 10. | 2.48 | 1.10 | 0.54 | 0.163 | 2.50 | 1.02 | 0.49 | 0.122 | Gray |
| 12. | 2.12 | 1.08 | 0.49 | - | 2.10 | 1.08 | 0.38 | - | Microscopic Mottles |
| 14. | 2.48 | 1.10 | 0.54 | | 2.37 | 1.06 | 0.42 | _ | White* |

off and sample removed as rapidly as possible.

The pickup of silicon has disappeared, a carbon and manganese loss persist, as before, sulphur is now removed and the tendency to freezing gray is reduced. The melts were quiet.

Since the melting and freezing thermal conditions were similar in the two series and since there is no particular reason for considering the deoxidation to be different, one is tempted to assign the increased graphitization, at least in part, to the effect of minute traces of zirconium.

Hedberg isolated the sonims for four of the materials by chlorination and determined the SiO₂, FeO, MnO and P₂O₅. We have recently become convinced that the metallic oxides cannot be isolated from graphitic material by this procedure so report only the acid radials.

TABLE 3

| Crucible | SiO ₂ | P ₂ O ₅ |
|-----------|------------------------------|--------------------------------|
| Zircofrax | None | 0.014 |
| ** | ** | 0.022 |
| Magnesia | 0.001 | 0.016 |
| Zircofrax | 0.005 | 0.019 |
| | Zircofrax ,,, Magnesia | Zircofrax None "Magnesia 0.001 |

As measured by SiO₂ present as sonims, deoxidation was practically complete.

As a check Wolff also made a few melts in air in zircofrax and in magnesia in a crucible furnace. Under these circumstances oxidation is quite extreme. Two typical heats were as follows:

TABLE 4

| | | | | T I I I I I | | | |
|-----------|--------|------|------|-------------|------|------|--------------------|
| Crucible | | | Comp | ositio | n | | Fracture |
| | Charge | | | Melt | | | |
| | C | Si | Mn | C | Si | Mn | |
| Zircofrax | 3.10 | 1.21 | 0.42 | 2.28 | 1.22 | 0.45 | Some mottles |
| Magnesia | 3.10 | 1.21 | 0.42 | 2.06 | 0.93 | 0.37 | Traces of Graphite |

We conclude that vacuum melting, accompanied presumably by the elimination of oxygen, contributes heavily to primary graphitization even though the effect of traces of zirconium is not necessarily to be ignored.

DISCUSSION BY C. L. ADOVASIO

C. L. ADOVASIO (Written Discussion): 4 Mr. Heine has done a wonderful job of further removing malleable iron production from the "art" field to the more exact "scientific" one.

Explanations for some of the perplexing variables observed in the past, probably will not go wanting long with the knowledge uncovered by Mr. Heine and to be revealed by future studies sure to follow from the stimulation of his findings.

Some work on the deoxidation of malleable iron was done at The Ohio Brass Company in 1937 under the direction of the late Fred L. Wolf. The work was instigated with hopes of finding means of improving annealability and physical properties. Pure aluminum wire and 25 per cent ferrotitanium were used for ladle additions at the furnace spout. The amount of Al and Ti additions were made and residuals of each in the iron were as follows:

| Heat No. | С | Mn | Si | Al Addition | Residual Al |
|------------------------|--------|----------------|--------|-------------------|-----------------|
| 4123 | 2.45 | 0.30 | 0.93 | 0.05 | 0.06 |
| 4152 | 2.70 | 0.27 | 0.93 | 0.10 | 0.108 |
| | | | | Ti Added | Residual Ti |
| 4123 | Same | as above | | 0.10 | 0.03 |
| 4152 | Same | as above | | 0.20 | 0.053 |
| 4153 | 2.53 | 0.29 | 0.94 | 0.30 | 0.078 |
| Heat No. | R | egular 4123 | | 4123 + A1 | 4123 + Ti |
| Y.P. | 33.300 | | | 30,300 | 32.450 |
| U.T.S. | 51,800 | | 48,800 | | 50,700 |
| Elongation | 19 | | | 18 | 20 |
| Fracture Appearance | Gi | ay rim | | Small gray rim | Small white rim |

⁴ Metallurgical Engineer. The Ohio Brass Co., Mansfield, Ohio.

| Heat No. | 4152 | 4152 + A | d 4123 + Ti |
|---------------------|--------|----------|-----------------|
| Y.P. | 31,050 | 29,500 | None |
| U.T.S. | 48,400 | 42,700 | 25,400 |
| Elongation | 13.5 | 10 | 1.5 |
| Fracture | | | |
| Appearance | Normal | Norma | Large white rim |
| Heat No. | | 4153 | 4153 + Ti |
| Y.P. | | 30,900 | None |
| U.T.S. | | 50,300 | 27,700 |
| Elongation | | 16.5 | 1.5 |
| Fracture Appearance | | Normal | Dull Gray |

I am not familiar with the method used to determine the residual aluminum. From the results of analyses it would indicate a complete absence of oxygen, which is very doubtful. However, from the results of the residual titanium analyses, one can assume that there existed enough oxygen in the iron to react with approximately 0.07, 0.147, and 0.222 per cent titanium respectively.

The physical properties of the test bars with the 0.06 per cent Al and 0.003 per cent Ti showed a slight reduction in yield point and tensile strengths but no appreciable difference in elongation. However, a small white rim was observed on the test bar treated with titanium which may have been similar to the phase," mentioned by Mr. Heine, resembling carbides.

Addition of 0.1 per cent Al reduced all the physicals as shown. Additions of 0.2 and 0.3 per cent Ti completely obliterated the yield point and reduced the elongations from 13.5 to 1.5 per cent and from 16.5 to 1.5 per cent respectively.

The fracture with the 0.2 per cent Ti addition showed a large white rim indicative of a much larger amount of the carbide like "phase."

The 0.3 per cent Ti addition produced a dull gray fracture, probably due to an excessive amount of mottling in the original white iron, to the extent that the "phase" mentioned was obscured

The latter tests with the reduction of physical properties were more drastic than those experienced by Mr. Heine, and should serve as a warning to foundrymen contemplating use of these deoxidizing alloys.

Incidentally, the difference in results between those shown here and those of Prof. Heine may, as suggested by him, be due to the residual aluminum content in the ferrotitanium.

AUTHOR'S REPLY TO DISCUSSIONS

PROF. HEINE (Author's Reply to Written Discussions): I would like to thank Mr. Tilley for his comments. There is no doubt but that oxidation variable influences the annealability of the iron. This was pointed out at some length in a paper which I presented at the 1949 A.F.S. Convention. (See "Some Effects of Deoxidation Treatments on Graphitization of White Cast Iron," by R. W. Heine, Transactions, A.F.S., vol. 57, pp. 315–331, 1949).

There is a question about the nodule count vs annealability. I pointed out that the addition of a small amount of aluminum in certain of these heats decreased the number of nodules. That might be taken as saying that the first bit of deoxidizer added lengthens the time required for first-stage graphitization. However, this is not true. The reduction of nodules in this case is a different phenomenon than that other reduction in nodule number from excessive oxidation. These irons were not excessively oxidized. They were normal air furnace melts.

It is a strange thing that although the nodule number decreases, the first-stage graphitization rate increases. I pointed out that in my first work, and Mr. Yarne who contributed a discussion to that paper tested that out in iron at the Chain Belt Co., and found a reduction in the annealing time of first-stage graphitization time from 30 hr to 15 hr. I appreciate Mr. Tilley's comments and thank him for them.

I very much enjoyed Mr. Rehder's comments. I am happy to see from the questions he has raised in his comments, that he realizes all the questions I had to ask myself before I wrote this paper. I think that each one of those questions, taken individually, can be completely answered.

The first question was in regard to the quantitative analysis for oxygen. There are two viewpoints which need to be consid-

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ered on this point. Analysis for oxygen in the iron and the study of the properties of the iron related to that oxygen content does not necessarily yield the same information as that which as attained by adding deoxidizers. Both methods of approach must be studied in order to gain a clear picture.

We are in process of constructing the analysis equipment for oxygen determinations. These things come slowly. The point is that there are different methods of studying the problem, each of which assists in providing a more complete picture.

In regard to Mr. Rehder's second question, he states that "The term deoxidation may be under some misinterpretation since to the steel maker it usually means decreasing the dissolved oxygen or FeO content." I do not quite follow this point. To the foundry steel maker deoxidation means adding a material which will react more powerfully with oxygen than carbon, silicon or manganese which are already present. That is the meaning which I have used in this work.

Mr. Rehder comments that carbon and silicon are effective deoxidizers. This has often been stated, but does not appear to be true in the case of silicon if we analyze the basic principles

of physical chemistry which are involved.

Curves showing decreasing oxygen with increasing percentage of carbon, silicon, or other deoxidizers in iron are familiar to most metallurgists. Such curves form the usual basis for stating that carbon and silicon are effective deoxidizers. However, in applying these curves to practical melting problems it is commonly overlooked that these curves are determined independently of each other, that is, silicon in iron in the absence of carbon, carbon in iron in the absence of silicon, and so on; furthermore, that they represent equilibrium conditions; and furthermore that they represent conditions where the products of the reaction are in equilibrium with the iron that contains these elements. The fact that cast iron is a complex alloy rather than a binary alloy which the oxygen content curves usually depict changes the situation entirely. As an example, the case of silicon may be considered. In order for silicon to deoxidize the melt, the reaction

$$Si + 2 O = SiO_2$$

must occur. However, at the melting temperatures at which a malleable furnace heat is finished in the vicinity of 2800 F, that reaction is not the reaction which predominates. The reaction

$$SiO_2 + 2C = Si + 2CO$$

prevents this from predominating and this reaction develops CO gas producing a boil. Carbon is able to reduce silica at these temperatures. There is no question about that being true both theoretically and practically. That also is the reason why the silicon content goes up at the end of an air furnace heat. Obviously, if you can reduce silica from the bottom of the furnace and cause silicon to go into the melt you can certainly reduce minute particles of silicon which are already in the melt and readily available to reduction by the carbon in the melt. So silicon is not affective as a deoxidizer at 2800 F since the reaction products by which it could function as a deoxidizer are reduced by the carbon in the melt. Thus it is carbon and only carbon which can keep oxygen down low in normal air furnace melting. The actual effectiveness of carbon depends on the attainment of

equilibrium. Amply presented in the literature, we find that in normal open hearth plants melting steel where the carbon reaction has been studied, the actual effectiveness of carbon is such that equilibrium is not reached and oxygen contents are usually higher than equilibrium. The actual oxygen content of the metal at air furnace melting temperatures is thus an end product of the ability of carbon to deoxidize to its lowest value, which it does-not attain, and which if reached is not nearly as low a percentage as deoxidizers such as aluminum could reach.

Mr. Rehder's questions about the skin effect, fluidity pinhole porosity, and not cracking are interesting from a practical viewpoint. No opportunity to study hot cracking arose. However, a number of thin sectioned castings were poured without any detectable difference in fluidity or misruns. No pinhole porosity was encountered although some heavier castings were poured.

The properties are much less affected when titanium is used because of its much milder deoxidizing effect. This milder effect is also noted in the inclusions; large additions being required to produce the change from Type I to the other types indicated.

Exception must be taken to the inference that the gentle boil observed in air furnace melting at temperatures around 2800 F is an equilibrium condition associated with the silica reduction reaction. The reaction

$$SiO_2 + 2C = Si + 2CO$$
,

is the cause of the boil only when non-equilibrium exists; the evolution of the CO bubbles showing that the reaction is progressing to the right. At equilibrium the reaction does not progress to right or left and the melt is quiescent. The evolution of CO is that which exceeds the solubility of CO in iron at the temperature, and thus clearly indicates that the melt is higher in oxygen content than what the equilibrium solubility of CO permits.

Mr. Rehder's comment about the higher percentage of carbon

being more apt to cause mottling certainly is true.

The author wishes to thank Mr. Adovasio for this contribution. In comparing effects of additions of deoxidizers it should be remembered that both the quantity of oxygen in the melt as well as the quantity of deoxidizer added will determine the total effect on physical properties.

Mr. Schwartz has produced further data which are needed for a thorough evaluation of this problem. The author has little to add to Mr. Schwartz' conclusions regarding the effect of deoxidation by vacuum meeting on primary graphitization. According to the literature the effect of zirconium as an alloying element is that of a strong carbide-former, similar to titanium. It is difficult to see how it would promote primary graphitization except through deoxidation.

Mr. Yarne's results with pretreatments are interesting. The author's opinion of the pronounced nucleating effects obtained are based on the tendency of deoxidized irons to graphitize below the critical temperature. Graphite nuclei may actually be developed at the subcritical temperature of the pretreatment; such nuclei growing on subsequent annealing. The work with physical properties provides further evidence of the influence of annealing cycles on properties.

In conclusion, appreciation is extended to all discussors for

their stimulating contributions and questions.

AN INTRODUCTION TO THE ANNEALING OF NODULAR IRON

By

J. E. Rehder*

ABSTRACT

Determination of the time-temperature relationships for the decomposition or graphitization of primary cementite and of pearlite in a typical nodular cast iron shows that the relationships are similar in nature to those previously found for normal white cast irons and black-heart malleable irons, and that the kinetics and mechanisms involved must be similar. Annealing times in general are not lengthy because of the relatively high silicon contents normally present in nodular iron, but the effect of the contained magnesium is shown in slowness of removal of last traces of cementite and pearlite. An unidentified inclusion was found to be associated with areas of persistent pearlite. Graphite precipitated during decomposition of primary cementite in nodular irons is shown to be in spherulitic form.

Mechanical properties of nodular iron bars with varying amounts of pearlite removed showed that removal of all pearlite is not necessary for the attainment of most of the ductility found in fully annealed, ferritic bars, although full annealing is essential for highest ductility.

Introduction

ALTHOUGH THE HIGH TENSILE STRENGTHS obtainable in nodular iron in the as-cast condition are in many cases very desirable, the greatly improved ductility, resistance to impact, and machinability which accompany absence of pearlite and primary carbide from the metallic matrix are frequently desired.

It is well known that under suitable conditions nodular iron can be made with a ferritic matrix as cast, the pearlite and carbide decomposing completely during cooling of the casting in the mold, but since for the present at least it would seem to be less costly to decompose existing pearlite and carbides by a subsequent annealing or heat treatment, a study of the annealing process would be timely. Such an investigation should be not only of immediate practical value, but should also throw light on the theory and mechanisms involved in the decomposition of carbides in these irons, whether during annealing in a furnace or during cooling of the casting in the mold.

It is the object of the present investigation, therefore, to study the process of decomposition of pearlite and primary carbide in nodular cast iron, to determine the type of reactions involved, the order of times and temperatures required, and the effect on mechanical properties of the removal of pearlite from the matrix. In this work, only nodular iron made by the magnesium process will be considered directly, but the principles should be applicable to cerium and other nodular irons.

Previous Work

The author knows of no publication dealing specifically with the annealing of nodular iron, although statements have been made in a few general papers that holding at subcritical temperatures for sufficient time will effect the decomposition of pearlite. That this should be so is obvious by analogy with the behavior of white iron and gray cast iron during similar treatment.

Since there is no discernible difference between white cast iron and gray cast iron in the nature of their reactions to heat treatment that cannot be accounted for on the basis of differences in known factors such as chemical composition, section size, graphite distribution, and so forth, there is no reason to suspect that the nature of the annealing process in nodular cast iron should be different in any way except one of degree. Therefore as a first approach all of the extensive literature on the kinetics and nature of the process of decomposition of cementite in malleable iron and gray cast iron should be of value. The present writer presented before this Society1,2 some generalizations on the annealing or graphitization process that seemed justified by his own work and a study of the literature, and the extent of agreement of the annealing process of nodular iron with this work will be described below.

Experimental Method

A 500-lb heat of nodular iron was made in an induction furnace from a charge consisting of pig iron, steel scrap, nodular iron remelt, and graphite, in about the proportions that would be used commercially in electric furnace or cupola melting. The molten iron was

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tapped at 2600 F (thermocouple measurement) into a preheated bull ladle to which 13 lb of iron-silicon-magnesium alloy containing 7 per cent magnesium was added just before tap-out. After filling the ladle, the reaction product was skimmed off and the iron poured into standard keel-block cores and into green sand molds which were to produce plates 10 in. by 8 in. by 1/4 in. thick. The as-cast section thickness of the legs of each keel block was 11/4 in. No post-inoculant was used in the ladle, as minimum production of primary cementite or chill was not desired. The chemical analysis of drillings from one of the keel block coupons is given in Table 1.

As part of another study, annealing times were determined on castings from another heat made with copper-magnesium addition. The furnace charge was as described above, but instead of iron-silicon-magnesium addition agent, 31/2 lb of 50-50 copper-magnesium alloy were added to the bottom of the empty preheated ladle before tap-out. After reaction, 4 lb of SMZ alloy were added as post-inoculant and the reaction product skimmed off. The iron was poured into, among other things, 50 molds of 1-in. pipe-fitting elbows which were made from a commercial match plate borrowed from a malleable iron foundry. Annealing studies were made on the heaviest section of the elbows, which was a 5/16 in. thick head, and these are the only pieces from this heat referred to in this paper.

The chemical analysis of drillings from a casting poured from this latter heat are given in Table 1.

TABLE 1

| Element | | Fe-Si-Mg Heat | Cu-Mg Heat |
|-------------------|------|---------------|------------|
| Silicon, " | 22 | 2.66 | 2.39 |
| Manganese, " | 20 | 0.40 | 0.41 |
| Total carbon, per | cent | 3.15 | 3.50 |
| Sulphur, " | 22 | 0.018 | 0.010 |
| Phosphorus, " | 9.9 | 0.051 | 0.051 |
| Copper, " | | 0.10 | 0.42 |
| Magnesium, " | 9.9 | 0.047 | 0.047 |

The silicon content of the iron before reaction with the iron-silicon-magnesium compound was 0.95 per cent, and the recovery of silicon from the addition agent was, therefore, 90 per cent. The sulphur content of the iron before reaction was 0.040 per cent, and the amount of desulphurization was therefore 55 per cent. The apparent recovery of magnesium from the addition agent (magnesium in iron over magnesium added times 100) was 15.7 per cent, and the over all recovery or utilization of the added magnesium including consumption of magnesium in desulphurization was 20.3 per cent. It should be noted that the recovery of magnesium was lower than usual for this addition agent. The nucleating, or effective, magnesium content⁸ (per cent retained magnesium less 0.75 times the residual sulphur content) was 0.033 per cent, which is just sufficient to make the structure fully nodular in the keel blocks.

The sulphur content of the iron of the coppermagnesium treated heat before reaction with the copper-magnesium addition agent was 0.026 per cent, and the amount of desulphurization was therefore 62 per cent. Apparent recovery of magnesium was 13.8 per cent, and over all utilization of magnesium was 17.3 per cent. The nucleating magnesium content was 0.039 per cent.

Since metallographic examination of castings from the iron-silicon-magnesium heat showed some primary cementite to be present in the keel-block coupons ascast (Fig. 1) which would affect determination of second stage annealing times (decomposition of pearlite), all of the keel-block coupons were heated to 1700 F, held at temperature for 3 hr, and air cooled. This removed the primary cementite with no detectable effect on the shape, size, appearance, or distribution of the graphite nodules, and the air cooling created a completely pearlitic matrix with no ferrite around the nodules.

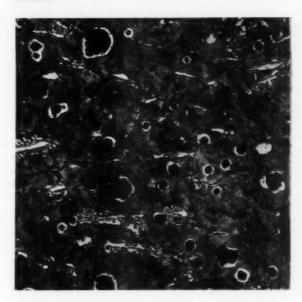


Fig. 1-Keel block coupon as-cast, X100, etched in 2 per cent nital.

Coupons 6 in. by 11/4 in. by 11/4 in. were cut from each leg of the keel blocks, and two of the coupons were cut into small samples approximately 3/4 in. by 1/4 in. by 1/4 in. by 1/4 in. with no material closer than 1/4 in. to an as-cast surface being used. The plates which were cast in green sand molds had 1/2 in. removed from the perimeter with an abrasive cut-off wheel and discarded, and the remainder was broken into pieces approximately 3/4 in. by 1/2 in. by 1/4 in. The small samples from the keel block coupons and the plates, and pieces from the beads of the 1-in. elbows, were used in the studies described below.

The procedures followed in determining minimum annealing times for decomposition of primary and pearlitic cementite were those described previously by the writer^{1,2,4} and need not be repeated in detail here. Prior to determination of second stage (pearlite) annealing times, plate and elbow as-cast samples were annealed at 1700 F for the determined times plus 10 per cent to decompose all primary cementite, and then air cooled to provide a completely pearlitic matrix. Temperature measurement and control during annealing was with a chromel-alumel thermocouple and

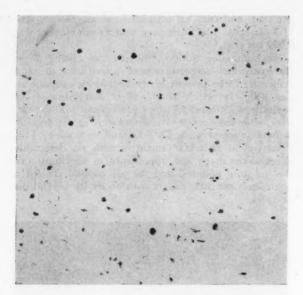


Fig. 2-1/4-in. plate as-cast, X100, unetched.

a program controller which controlled within plus or minus 2 F. Temperatures were checked periodically with a calibrated platinum-platinum rhodium thermocouple and precision potentiometer. Over all accuracy of control and measurement of temperature is believed to have been within plus or minus 3 F.

The maximum cooling rates through the critical temperature range which would remove all pearlite from the keel block coupon, plate, and elbow samples, were determined on samples previously given first stage annealing; by holding 2 hr at 1650 F, and then cooling at a controlled rate to 1330 F. This procedure was repeated for different cooling rates, using fresh samples each time, until the maximum permissible cooling rate was determined for each sample which provided a completely ferritic matrix.

To determine the effect of annealing time and microstructure on mechanical properties, ten keel block coupons were annealed in a vapocarb furnace with no gas added, and control of temperature was

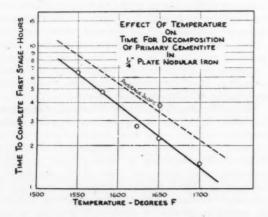


Fig. 4-Effect of temperature on time for decomposition of primary cementite in 1/4-in. plate.



Fig. 3-1/4-in. plate as-cast, X100, etched in 2 per cent nital.

plus or minus 5 F. The ten coupons were heated in the furnace to 1650 F, held at temperature for 3 hr, and cooled in the furnace at 200 F per hr to 1340 F. During subsequent holding at 1340 F, two coupons were taken from the furnace at intervals and air cooled. Standard 0.505-in. diam tensile test bars were machined from each coupon, and the mechanical properties of each bar determined. Metallographic specimens were cut from each of the broken tensile specimens for estimation of amount of pearlite present, which was done by planimetering of photographs of several fields from each sample. Subsequently, two further coupons were heated to 1650 F, held at temperature for 3 hr, cooled to 1550 F in 20 min, and cooled from 1550 to 1330 F at a controlled rate of 20 F per hr. Mechanical properties and microstructure of tensile test bars machined from these coupons were then determined.

Results

First-Stage Annealing—This concerns the decomposition of primary or massive cementite, usually above the critical temperature. In the samples made as described above, and used in this study, some primary cementite was present in the keel block coupons, and of course, a considerable quantity was present in the 1/4-in. plates. A suitable amount of appropriate post inoculation would have eliminated the carbide from the keel block coupons and decreased the amount in the plates, but for the present purpose this was not desired. The as-cast structure of the keel block coupons is shown in Fig. 1, and of the as-cast plates in Fig. 2 and 3. It is evident from Fig. 2 and 3 that although some free graphite formed as nodules in the plate, the amount was not large and considerable primary carbide is present. The as-cast microstructure of the 1-in. elbow samples was similar to that of Fig. 2 and 3.

The times necessary to decompose all of the pri-

mary cementite in the plate samples at different temperatures are given in Table 2, and are plotted as a semi-logarithmic graph in Fig. 4. In Table 2 is also included the number of nodules formed per unit area at each temperature.

Table 2—Time to Decompose Primary Cementite in Plate Samples

| Temperature, F | Time, hr | Nodules, No/sq mm |
|-------------------|-------------|----------------------|
| 1700 | 1.5 | 191 |
| 1650 | 2.25 | 187 |
| 1625 | 2.75 | 189 |
| 1580 | 4.75 | 188 |
| 1550 | 6.50 | 195 |

The time for decomposition of the primary cementite in the elbow samples was determined at one temperature of 1625 F to be 61/2 hr, with a nodule count of 254 per sq mm.

No significant difference was noted in the number of nodules present in the plate samples after decomposing primary cementite at different temperatures, and since the small number already present in the as-cast samples was, of course, constant, a uniform number of nodules were created or grew at each temperature and corresponding time. No detectable difference was observed in the structure, shape or distribution of nodules formed at different temperatures, and as a result, differences in mechanical properties should be small. A feature of the nodules formed is most interesting from both practical and theoretical viewpoints. The nodules found in the plate and elbow samples after complete decomposition of primary cementite were without exception spherulitic in structure as in normal nodular iron free of primary cementite as cast, and in no case showed evidence of flake-aggregate type of structure as in annealed black-



Fig. 5-Nodules in ½ in. plate after 2½ hr at 1650 F, X750, lightly etched.

heart malleable iron. As a result the nodules are smooth surfaced, which is believed to be a contributing factor to high mechanical properties and of consequent practical importance. The theoretical implications will be discussed below. Typical nodules formed are shown in Fig. 5.

An incidental observation is that in determining minimum times for complete decomposition of primary cementite, samples were at first water-quenched on being withdrawn from the furnace for determination of the amount of primary cementite remaining after various intervals of time at temperature. It was found that such water-quenched samples were largely austenitic in the dendritic areas between the carbide masses, leading to difficulty in detecting small amounts of cementite. The dendritic areas were clearly delineated by the retained austenite, and the effect is believed due to micro-segregation of silicon in the dendrites, leading to retention of austenite or high hardenability in these areas. This effect was noted previously on a lesser scale in malleable iron samples, and was described by the present writer.2 Detection of traces of remaining primary cementite was made easy by air cooling the samples from the furnace to prevent the retention of austenite, and etching with hot alkaline sodium picrate.

It was noted in all samples that the last traces of primary cementite were slow to disappear, much more so than is the case with normal white irons and malleable irons, and approximately the last half of the total time necessary is consumed in removing the last traces of carbide. The complete removal of the primary cementite is important, since any traces remaining act as centers of pearlite stabilization in subsequent second stage annealing. Even if decomposition or elimination of pearlite is not subsequently desired, the presence of traces of hard, brittle cementite in the matrix will have an adverse effect on machinability and probably on impact strength and ductility.

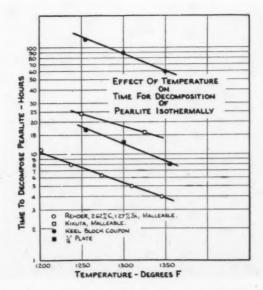


Fig. 6-Effect of temperature on time for decomposition of pearlite.

Second Stage Annealing—This concerns the decomposition of pearlitic cementite so as to leave a completely ferritic matrix, either by holding at subcritical temperatures or by relatively slow cooling through the critical temperature range. The results of determination of the effect of temperature on the time necessary to decompose all pearlite at constant subcritical temperature are given in Table 3 and Fig. 6 for the plate, keel block, and elbow samples.

TABLE 3-EFFECT OF TEMPERATURE ON TIME FOR DECOMPOSITION OF PEARLITE

| Temperature | T | | |
|-------------|---------------|--------|-------|
| F | 1/4 in. plate | coupon | elbow |
| 1355 | 8 | 60 | 25 |
| 1300 | 13 | 90 | - |
| 1255 | 17 | 120 | _ |

Included in Fig. 6 is data from Kikuta⁵ and from unpublished work of the writer on the isothermal decomposition of pearlite in malleable irons.

In Fig. 7 and 8 are shown samples of the plate and coupon after complete decomposition of pearlite.

In Table 4 are given the results of determination of the maximum cooling rate through the critical temperature range that will remove all pearlite from the subject samples. In the case of the coupon and elbow samples, use of a cooling rate 5 F per hr faster than that shown resulted in the presence of small traces of pearlite. Since the maximum controlled cooling rate possible with the 'equipment used was 105 F per hr, and the plate sample was free of pearlite at this rate, the maximum rate possible for this sample could not be determined. During cooling at 10 F per hr, the start or top of the critical temperature range for the coupon and plate samples was determined approximately as 1490 F.

The graphite resulting from decomposition of

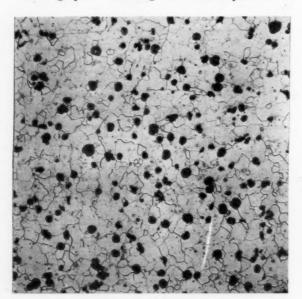


Fig. 7-1/4-in. plate, fully annealed, X100, etched in 6 per cent nital.

TABLE 4-MAXIMUM COOLING RATES FOR ELIMINATION OF PEARLITE

| Sample | Deg I | per hr | |
|-------------------|-------|--------|--|
| 1/4-in. plate | | plus | |
| Coupon | 25 | | |
| Elbow | 40 | | |

pearlite isothermally or from slow cooling through the critical temperature range precipitates on the existing nodules, as is the case during the annealing of malleable iron. Usually this "secondary" graphite is visible in nodular iron as rings around the pre-existing nodules, since the graphite frequently precipitates as flake-aggregate type graphite and as such is easily distinguished from the spherulitic pre-existing graphite by appearance and by reaction under polarized light. However, in the present samples this was not always the case, the secondary graphite being frequently of structure similar to the pre-existing nodule and distinguishable only by a line of demarcation. This is shown in Fig. 9. Decomposing pearlitic cementite by slow cooling through the critical temperature range apparently usually gives this type of precipitation, while holding at subcritical temperature more usually gives the flake-aggregate type of precipitation.

In Fig. 10 is shown the flake aggregate type of secondary graphite. A point of interest in this photomicrograph is that the original sphere of spherulitic graphite as been pulled out of one of the nodules during polishing, indicating that the bond between the two types of graphite structure must be poor.

It was noted in the plate and elbow samples that in no case was secondary graphite in flake aggregate crystallization, and this indicates that in these samples the secondary graphite precipitated directly on to the pre-existing graphite structure.

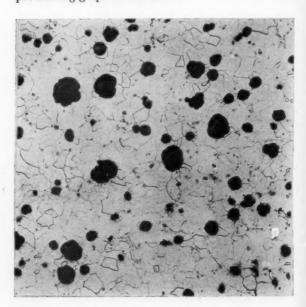


Fig. 8-Keel block coupon sample, fully annealed, X100, etched in 6 per cent nital.

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It was observed that regardless of the type of secondary graphite precipitation, the thickness of the layer of such graphite varied inversely as the size of the original nodule, larger nodules having thinner rings of secondary graphite than smaller nodules. This was most noticeable in the extremes, the thickness of the layer on very small nodules being several times that of the layer on a large nodule.

As in the decomposition of primary cementite where the last traces of cementite were found to be slow to disappear, in the decomposition of pearlitic cementite isothermally below the critical temperature range the last traces of pearlite were very slow to decompose. In all cases most of the pearlite disappeared in approximately the first third of the total time necessary, while continued time at temperature was necessary for the complete removal of all traces of pearlite. Again this effect was much more pronounced than is the case with normal white cast irons and malleable irons.

Examination of areas of persistent pearlite showed that in most, if not all, cases, an unusual inclusion was present in the pearlite. This type of inclusion has been noted in nodular irons many times previously by the writer, but attempts to identify it have so far



Fig. 9-Secondary graphite in apparently spherulitic crystallization, X750, etched in 6 per cent nital, photographed in polarized light.

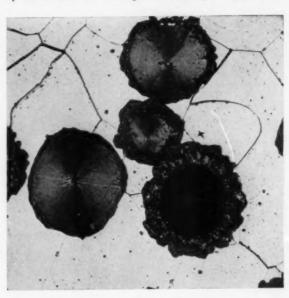


Fig. 10—Secondary graphite in flake aggregate crystallization, X750, etched in 6 per cent nital.

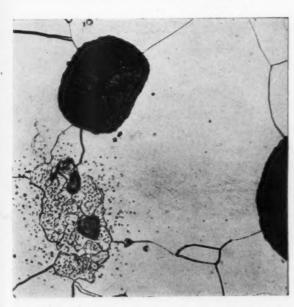


Fig. 11-Inclusions in persistent pearlite, X750, etched in 6 per cent nital.

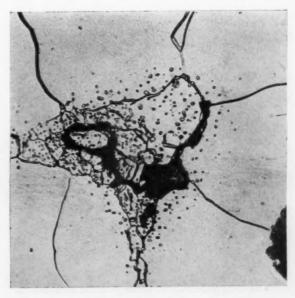


Fig. 12-Inclusions in nodular iron, similar to those in Fig. 11, X750, etched in 6 per cent nital.

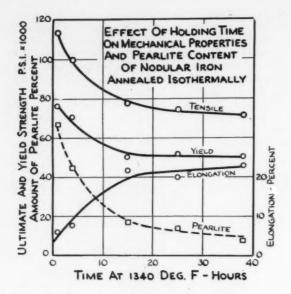


Fig. 13—Effect of time of holding at constant subcritical temperature on mechanical properties of nodular iron.

met with no success. The inclusion is easily torn out in polishing, and the utmost care is necessary to retain any for examination. In color it is dark brown or black, and is apparently allotriomorphic in crystal habit. The inclusions are isotropic under polarized light. They are apparently decomposed by water, since dry polishing or polishing in alcohol helps retention, but this is not known definitely. The inclusions are relatively numerous, occurring usually in clumps. In Fig. 11 is shown an area of pearlite in which the inclusions may be seen, or rather the holes where they were, and in Fig. 12 is shown a photograph of a similar inclusion in another nodular iron. These

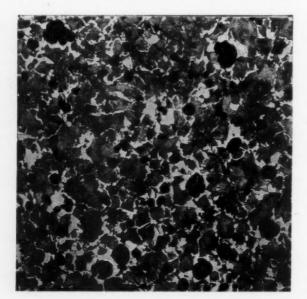


Fig. 14-Keel block coupon held 1 hr at 1340 F, X100, etched in 6 per cent nital.

inclusions apparently have a stabilizing effect on the decomposition of pearlitic cementite, presumably through a solid solution effect. The inclusions are apparently little affected by annealing treatment, and were detectable in all cases in the fully anealed irons.

Mechanical Properties

The results of determination of mechanical properties of bars which contained different amounts of pearlite in the matrix are given in Table 5. Tests were made and reported on duplicate bars, the duplicate tests agreeing satisfactorily. In Fig. 13 the average of the results of each pair of bars is plotted, and in Fig. 14 to 18 inclusive microstructures of samples cut from broken tensile specimens are shown. The microstructure of the slow cooled sample was indistinguishable from that of Fig. 8.

TABLE 5—EFFECT OF ANNEALING TIME AND AMOUNT OF PEARLITE ON MECHANICAL PROPERTIES OF NODULAR IRON ANNEALED ISOTHERMALLY

| | t Tensile Strength, psi | Yield Strength, psi | Elonga- tion, Per Cent | Area, | Brinell Hardness Number | Pearlite |
|--------|-------------------------------|---------------------------|------------------------------|-------|-------------------------------|----------|
| 1 | 114,000 | 75,800 | 6.5 | 4.0 | 229 | 67.0 |
| | 113,100 | 77,100 | 5.5 | 3.5 | 229 | |
| 4 | 99,700 | 68,200 | 8.0 | 6.3 | 197 | 44.8 |
| | 100,100 | 73,100 | 7.5 | 5.1 | 197 | |
| 15 | 77,700 | 54,000 | 20.5 | 21.6 | 197 | 16.6 |
| | 78,300 | 51,500 | 22.0 | 21.6 | 197 | |
| 25 | 74,500 | 53,000 | | _ | 179 | 14.0 |
| | 74,900 | 51,000 | 20.0 | 20.2 | 179 | |
| 38 | 71,600 | 49,500 | 23.0 | 21.6 | 179 | 7.7 |
| | 71,700 | 51,500 | 23.0 | 26.8 | 179 | |
| Slow | 68,900 | 49,800 | 27.0 | 30.3 | 167 | 0 |
| Cooled | 69,400 | 49,500 | 28.0 | 30.9 | 167 | |

It will be noted in Table 5 that the elongation and reduction of area are approximately numerically equal for all of the bars tested, and this has been found in

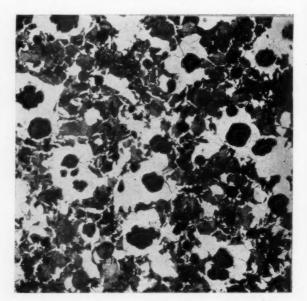


Fig. 15-Keel block coupon held 4 hr at 1340 F, X100. etched in 6 per cent nital.

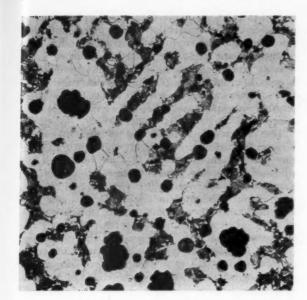


Fig. 16-Keel block coupon held 15 hr at 1340 F, X100, etched in 6 per cent nital.

the past to be a consistent feature of nodular iron tensile bars whether as cast or annealed.

Effect of Manganese Content

Although no specific work has been done to the writer's knowledge on the effect of manganese content on the rate of decomposition of primary and pearlitic cementite in nodular irons, there appears to be no reason to assume that the results of previous work on white irons and malleable irons are not applicable. The writer showed4 that the effects of manganese and sulphur on rate of graphitization are interrelated, as was well known, and that the manganese present in excess of that necessary to neutralize the sulphur as shown to have little effect on the rate of decomposition of primary cementite until relatively large amounts were present, but that excess manganese had a powerful stabilizing effect on pearlitic cementite.

Previously, the effect of manganese on the rate of decomposition of pearlitic cementite had been investigated by Forbes, Paulson, and Minert,8 who gave quantitative results. When the data of Forbes et al were replotted, and compared with the quantitative effects of manganese content noted by the writer,4 good agreement was found, and the results may be summarized in the statement that other factors remaining the same, the time necessary for complete decomposition of pearlitic cementite is approximately doubled for every increase of 0.15 per cent of manganese above that necessary to neutralize the effects of sulphur.

The amount of manganese necessary to neutralize the effects of sulphur was shown by the writer⁴ to be the stoichiometric amount to form MnS with all of the sulphur present, plus 0.15 per cent. This was for the range 0.06 to 0.20 per cent sulphur, since nearly all commercial black-heart malleable irons lie within

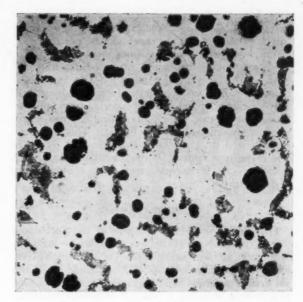


Fig. 17-Keel block coupon held 25 hr at 1340 F, X100, etched in 6 per cent nital.

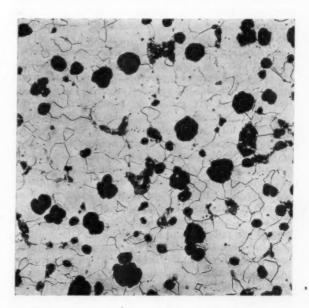


Fig. 18-Keel block coupon held 38 hr at 1340 F, X100, etched in 6 per cent nital.

this range. The 0.15 per cent of manganese necessary over the stoichiometric amount is believed due to mass law effects, and with the very low sulphur contents normally present in nodular iron, this factor would be lower. It is not known precisely what excess of manganese over that necessary stoichiometrically will chemically or kinetically neutralize the sulphur in very low-sulphur-content irons, but it is probably small, and much less than 0.15 per cent. The formula used in the malleable industry to determine the optimum manganese content for a given sulphur content is, therefore, not applicable to nodular irons, and

there is evidence to suggest that the excess manganese necessary is of the order 0.02 to 0.04 per cent when the sulphur content is of the order of 0.01 per cent.

The conclusion from the above discussion is that a given amount of manganese in a nodular iron will have considerably greater effect on the amount of pearlite retained as-cast, and on the rate of decomposition of pearlite, than would be expected from familiarity with gray cast irons and malleable irons where sulphur contents are much higher. This has been corroborated in a general way by the fact that very low manganese content is one essential of a ferritic matrix as-cast in nodular iron with accompanying high ductility.

Discussion of Results

First Stage Annealing-Progress of the decomposition of primary cementite was noted to be the same kinetically as that taking place when annealing normal white irons and black-heart malleable irons, and the results obtained are interpreted in the same way. It is evident from Fig. 4 that the relationship between time and temperature of decomposition is essentially straight when plotted semi-logarithmically, and also that this line is of a slope similar to the average slope previously found1 for various samples of white iron and malleable iron. The similarity of slopes is considered within the probable limits of error of the determination, and the slopes may be considered identical for practical purposes. Inspection will show that for every 75 F increase in temperature, the time necessary to decompose all primary cementite is approximately halved. It should be emphasized that the relationship shown in Table 2 and Fig. 4 is strictly true only for the samples concerned, and that the actual position of the line, or its intercept, will depend as for malleable iron on silicon content, other constituents, as-cast section size, amount of carbide present, heredity effects, and other effects listed elsewhere. However, there is no reason to suppose that the slope of such a line should be appreciably different from that shown, and therefore the experimental determination of one point should locate the line.

It has been shown clearly^{5,6,7} that as section thickness as-cast increases, the time necessary to decompose primary cementite increases, and it has been shown by Schneidewind⁶ that the time necessary is related to the as-cast section thickness through the effect of cooling rate in the mold on the size and arrangement of primary cementite masses and the resulting specific cementite-austenite interface. Such relationship has been found to be true for nodular irons, and relatively small amounts of carbide in a thick section may take longer to decompose than larger amounts in a thin section. This must, of course, be taken into account in practice. It has been found by the writer in a previous unpublished study that the primary cementite in a chilled nodular iron (cast against a metal chill) can be decomposed rapidly, due to the finegrained distribution of cementite, and this fits in well with known data on other white and malleable irons as described above.

The fact that the elbow samples required a longer time at a given temperature for complete decomposition of primary cementite than did the plate samples is considered of no particular quantitative significance, since the time necessary is affected by chemical composition, as-cast section size, and other factors, which were different for the plate and the elbow samples. It is probable that the longer time necessary for the elbow samples was due largely to the higher nucleating magnesium content and lower silicon content of these samples.

The observed fact, illustrated in Fig. 5, that the nodules formed on annealing are spherulitic in structure, leads to the hypothesis that not only must specific nuclei have been present in the solidified iron, but that the desirable spherulitic type of nodule grew upon them. The nucleus responsible has not been identified, and the substantiation of this hypothesis should provide a broad field for further studies. There has been conjecture in some quarters that the formation of nodules of spherulitic structure in as-cast nodular iron is due to surface tension effects in the solidifying metal, presumably as modified by the presence of magnesium or cerium. This is a difficult hypothesis to support, and the facts described above would seem to remove all support from the surface tension hypothesis, or at least relegate the effect of surface tension to a secondary role. The writer, needless to say, presently adheres to the hypothesis that formation of nodules of spherulitic structure is due primarily to the presence of suitable nuclei in the correct range of subdivision of size.

Effect of Composition

The effects of chemical composition of the iron, other than magnesium content, on the rate of decomposition of primary cementite may be inferred with some accuracy from known effects in normal white iron and malleable iron. The accurate determination of the effect of magnesium content alone on graphitization rates is beset with considerable experimental difficulties, and specific data are not at present available, but is believed tentatively that the effect of silicon content should be similar to that previously noted,1 the logarithm of the silicon content when plotted against the logarithm of the time necessary to decompose all primary cementite being a straight line, shorter annealing times of course accompanying higher silicon content. The relatively high silicon content of nodular irons is the principal reason the primary cementite is so readily decomposed as compared with malleable iron.

The effect of carbon content is expected to be similar to that for malleable iron. Phosphorus stabilizes pearlitic cementite to an appreciate extent, but in most nodular irons is sufficiently low for other reasons that its effect on first stage graphitization is probably negligible.

The effect of manganese content on the rate of decomposition of primary cementite is, as described above, minor or negligible except when unusually large amounts are present. When considerable manganese is intentionally present in nodular irons to stabilize pearlitic cementite, some effect of its presence in amounts greater than about 0.50 per cent may be noted if primary cementite occurs and must be removed by annealing.

Second Stage Annealing

It is apparent on inspection of Fig. 6 that the relationship between time necessary for decomposition of pearlite isothermally and the temperature used is semi-logarithmic within the limits of experimental error. Furthermore, there is a relatively small difference in the slopes of the lines representing the two samples of nodular iron and the two malleable irons, which indicates that the kinetics of the reactions, as would be expected, are similar. The average slope of the lines is such that for every 100-deg increase in temperature, the time necessary for isothermal decomposition of pearlite is approximately halved. It should be noted that 1350 F is considered the highest temperature that can safely be used for isothermal annealing in commercial furnaces.

The plate and coupon samples are of identical source and chemical composition, although the time necessary to decompose the pearlite at any given temperature is considerably longer for the coupon samples. This is believed to be due to the difference in size of dendritic as-cast structure and the number of nodules present per unit area or volume. Such differences in time necessary for decomposing pearlite with differences in number of nodules are common in the malleable iron field. This factor is of importance in the commercial production of nodular iron, since with different addition agents and different techniques, nodule counts can be varied considerably. Smaller and more numerous nodules will result in more rapid decomposition of pearlitic cementite, other factors remaining the same, but by analogy with malleable iron practice, some compromise must be made since the best mechanical properties are not obtained with a high nodule count.

The data in Table 4 shows that pearlite can be completely eliminated in shorter overall time by slow cooling through the critical temperature range than by holding at subcritical temperatures, which agrees with data on the annealing of malleable iron. The actual maximum cooling rate possible will, of course, depend on silicon content, nodule count, and other factors. The difference in maximum permissible cooling rates as affected by as-cast section size and nodule count alone is evident from the differences in Table 4 between the coupon and the plate samples. The elbow samples, which are from a different heat, with different nodulizing addition agent and different chemical composition, cannot be directly correlated with the other samples except in such general terms as noting that while the thicker as-cast section size, higher nucleating magnesium content, and lower silicon content of the elbow samples would provide slower permissible cooling rates than the plate sample, the higher nodule count should provide faster permissible cooling rates, with the net result not predictable at present.

No complete explanation is at present available for the differences noted in the type of secondary graphite crystallization, especially of its almost complete lack of visibility in the plate specimens. In the case of samples where pearlite has been eliminated by slow cooling through the critical temperature range, it is possible that the secondary graphite has deposited in apparent continuation of spherulitic form because in such case deposition of graphite is largely from austenite, rather than from ferrite.

The fact that the rings of secondary graphite are thinner on large nodules may be explained as either the result of sectioning nodules at various distances from their centers, those sectioned far from the center having an apparently thicker secondary graphite layer; or as the result of carbon precipitating uniformly from the matrix during second stage graphitization onto existing nodules considered as single nuclei, in which case the same amount or weight of graphite would be deposited on each existing nodule, resulting in thicker rims on smaller nodules.

The unidentified inclusions noted in the present and other samples of magnesium-treated nodular irons are, as mentioned above, difficult to study because of the ease with which they are dislodged or decomposed during polishing. Their presence definitely inhibits the decomposition of pearlite. It is considered probable that they are some compounds of magnesium, and possibly a magnesium carbide which is affected by water. If eventually proven to be a magnesium carbide, their presence would account for the distinct odor of acetylene noticed on freshly broken surfaces of nodular cast iron.

Effect of Composition

The effects of chemical composition, other than magnesium content, on the rate of decomposition of pearlitic cementite are, as mentioned for first stage annealing, probably the same as for normal white irons and malleable irons. The rate of second stage graphitization will increase with increase in silicon content, and also with increase in carbon content since in the latter case higher carbon content produces shorter diffusion distances for the carbon atoms. Phosphorus content is usually sufficiently low in nodular irons that its effect on rate of second stage graphitization should be minor or negligible.

The manganese content will have a marked effect on the rate of second stage graphitization, as described above, and the effect will be larger than expected unless the very low sulphur content is taken into account. A gray cast iron containing 0.100 per cent sulphur and 0.50 per cent manganese will have approximately 0.17 per cent manganese effective as a retardant of graphitization, whereas a nodular iron containing 0.010 per cent sulphur and 0.50 per cent manganese will have approximately 0.47 per cent manganese effective as retardant. If every change in effective manganese content of 0.15 per cent alters the rate of second stage graphitization by 100 per cent, then the time for decomposition of pearlitic cementite in the nodular iron would be about four times that of the gray cast iron, other factors equal. If the presence of magnesium further retards decomposition of pearlite, as appears probable, then the importance of low manganese content for rapid annealing of nodular iron is apparent. If the manganese content is sufficiently low (about 0.05 per cent, and the carbon and silicon contents are high, a nodular iron should graphitize completely while cooling in the mold after pouring, which is

actually the case. If a completely pearlitic structure, with no ferrite present, is desired in nodular iron for high strength, then the manganese content should be in the range 0.35 to 0.50 per cent, and possibly higher for heavy sections. This is especially true if some primary cementite or chill must be removed by annealing with a completely pearlitic matrix desired on air cooling.

In the case of the coupon and plate samples described herein, the manganese content effective as a retardant of second stage graphitization would be approximately 0.32 per cent, and decreasing of the manganese content to 0.05 to 0.07 per cent should decrease the isothermal second stage annealing times to about one third of the times shown in Table 3 and Fig. 6, with corresponding increase in permissible

cooling rates.

Experience in the annealing of iron-carbon-silicon alloys has shown, and the present data corroborates, the fact that white cast irons, gray cast irons, malleable irons and nodular irons cannot be annealed by rule of thumb or by calculation from a table of factors, because so many poorly understood phenomena are involved. Mention of heredity effects alone will demonstrate this statement. The annealing or graphitization of nodular irons will follow many principles already elucidated, but the most efficient, lowest cost commercial annealing cycle for a given nodular iron in a given foundry will still be a matter of experimentation and control.

The annealing cycles possible for nodular iron are much shorter than those feasible for black-heart malleable iron, and annealing costs will, therefore, be lower, by a factor or 25 to 50 per cent depending on conditions. For the keel block coupons described above, a practical cycle would be as in Table 6, in which time is provided for removal of the small quantity of primary cementite present as-cast. In all of the annealing cycles to be described, it is apparent from the foregoing discussion that a decrease in manganese content or an increase in silicon content, or both, would decrease considerably the total annealing times required.

TABLE 6-ANNEALING CYCLE FOR PRESENT KEEL
BLOCK COUPONS-NO. 1

| | Heat to 1700 F | 4 | hr |
|---|-----------------------------|-----|-------|
| | Hold at 1700 F | 3 | 99 |
| | Cool to 1550 F | 1/2 | ** |
| | Cool from 1550 to 1330 F at | | |
| | 25 F per hr | 9 | 99 |
| - | | _ | |
| | Total | 161 | /2 hr |

If a suitable amount of post-inoculant had been used to eliminate the primary cementite from the ascast bars, the above cycle could be decreased by 2 hr, allowing 1 hr at 1700 F for equalizing temperature.

TABLE 7-ANNEALING CYCLE FOR PRESENT 1-IN. ELBOWS

| Heat to 1700 F | 4 hr | |
|--------------------------|--------|--|
| Hold at 1700 F | 3 " | |
| Cool to 1550 F | 1/2 " | |
| Cool from 1550 to 1330 F | | |
| at 40 F per hr | 51/2 " | |
| Total | 13 hr | |
| 1 Otal | 13 111 | |

For the 1-in. pipe fitting elbows, a full annealing cycle would be as in Table 7.

If an annealing furnace charge were made up entirely of castings such as the 1/4-in. plate described above, full annealing could be accomplished in the cycle given in Table 8.

Table 8—Annealing Cycle for Present 1/4-In.
Plate Samples

| Heat to 1700 F | 4 hr | |
|--------------------------|---------|--|
| Hold at 1700 F | 2 " | |
| Cool to 1550 F | 1/2 " | |
| Cool from 1550 to 1330 F | | |
| at 100 F per hr | 21/4 hr | |
| | | |
| Total | 83/4 hr | |
| | | |

Naturally, in commercial practice an annealing furnace load will consist of castings from various heats and of different section thicknesses and the annealing cycle used must be that which fully anneals the heaviest section of lowest silicon content and highest manganese and magnesium contents. It is then a question of good foundry control practices to maintain optimum conditions so that a uniform product will be obtained, which of course is prerequisite for any well-conducted foundry operations.

Mechanical Properties

Results of mechanical tests of bars that were in various stages of completeness of removal of pearlite are most interesting, in that it is shown that a large proportion of the maximum ductility or elongation obtainable is present when there is still an appreciable amount of pearlite in the microstructure. This is important from the commercial viewpoint, since in these irons in which the last traces of pearlite are slow to disappear, the retention of a small amount of pearlite apparently does not drastically affect ductility as it would in other materials. It should be emphasized, however, that in all cases complete elimination of pearlite should be the objective, since then the optimum combination of strength and ductility are obtained, and impact strength and machinability will be at their maxima.

It is of interest to note that the mechanical properties obtained on the fully annealed iron are of great practical interest in themselves, since the combination of properties shown with the excellent machinability obtainable, places this material in an advantageous position in obtaining an optimum combination of properties at lowest over all cost.

Conclusions

The following conclusions would seem to be justified, based on the data obtained and the discussion provided.

1. The process and kinetics of the decomposition of primary and pearlitic cementite during the annealing of nodular iron are similar to, or the same as, those for the annealing of normal white cast irons and malleable irons, with the metallographic difference that the graphite precipitates in spherulitic, rather than flake-aggregate form.

2. The differences apparent in the times necessary

for annealing nodular iron and malleable irons are considered explainable on the basis of differences in chemical composition, although quantitative correlations are not at present possible.

3. The last amount or traces of cementite or pearlite in nodular irons containing magnesium are per-

sistent, and are relatively slow to disappear.

4. An unidentified nonmetallic inclusion is present in considerable quantity in magnesium-treated nodular iron, which is apparently a powerful carbide stabilizer.

5. As is the case for malleable and gray cast irons, minimum overall annealing time is obtained by eliminating pearlite by slow cooling through the critical temperature range, rather than by isothermal subcritical annealing.

The total annealing time necessary for a typical nodular iron is considerably shorter than is necessary

for malleable iron.

7. The retarding effect of manganese on rates of graphitization is more obvious with nodular iron because of the very low sulphur contents obtaining. Low manganese content is essential for minimum annealing times or for ferritic structure as cast.

8. The presence of some pearlite in the matrix does not seriously decrease the elongation obtained in tensile testing of nodular iron, as it does in malleable iron. Maximum elongation and optimum properties are, of course, obtained with a completely ferritic matrix.

9. No differences in behavior or kinetics during annealing were detectable between heats treated with iron-silicon-magnesium alloy and with copper-magnesium alloy, except one of degree considered explainable on the basis of differences in composition.

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DISCUSSION

Chairman: H. Bornstein, Deere & Co., Moline, Ill.

Co-Chairman: T. E. EAGAN, Cooper-Bessemer Corp., Grove City. Pa.

B. F. Brown (Written Discussion): The field of foundry metallurgy is largely one in which much descriptive investigation is needed; dependable experimentation of the sort for which Mr. Rehder is becoming noted is certainly to be welcomed.

The physical metallurgical principles involved in the three cast iron annealing reactions treated in this investigation are not obscure, but they might well be stated here explicitly in the interest of a more concise evaluation of this and other studies on the subject:

1. First stage or gamma-range graphitization. This reaction, by which the massive eutectic (and other) carbides are decomposed at temperatures above the critical, may be written as

cementite _____ austenite + graphite.

It occurs by a process of nucleation of graphite nodules and their growth. This growth is accomplished by the addition of carbon which originates from decomposing cementite and which diffuses through austenite to deposit on the growing nodules; the diffusion potential in the austenite under which the movement of carbon takes place is given by the separation of the $A_{\rm em}$ and the $A_{\rm gr}$ lines of the iron-carbon diagram (Fig. A). In

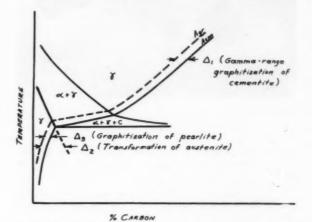


Fig. A-Vertical section through the Fe-C-Si diagram showing diffusion potential gradients involved in the three annealing reactions.

order to determine whether the mechanism involved in the gamma-range graphitization of nodular iron is identical with that of first stage malleabilization, it will be necessary first to measure the size distribution of the nodules at various times during the reaction and compare them with similar data from first stage malleabilization studies, and second to observe the manner in which the carbides in the immediate vicinity of the growing nodules disappear.

After the complete decomposition of the massive carbides by gamma-range (first stage) graphitization, or after austenitizing any nodular iron for sufficient time, the austenite will contain a minimum of about 0.65 per cent carbon—eutectoid composition.

The conversion of this eutectoid austenite to a ferritic matrix may be either direct (reaction 2 below) or indirect (reaction 3 below).

2. Direct transformation of austenite. When austenite of eutectoid composition is cooled below the upper eutectoid temperature, it may transform according to the reaction

austenite - ferrite + graphite.

In nodular iron this graphite customarily forms on the existing nodules, and the ferrite may form either in growing shells about the graphite nodules (Fig. 14 to 18), or it may form largely first in the austenite grain boundaries. As the austenite continues to decompose, its carbon atoms diffuse through the ferrite to plate out on the nodules and its iron atoms attach to the ferrite lattice. This reaction may continue until the matrix is converted completely to ferrite.

3. Subcritical graphitization of cementite. Eutectoid austenite in nodular iron may be induced to transform to pearlite by rapid cooling, by isothermal transformation at temperatures well below the eutectoid range, or by other means. The cementite of this pearlite can then be decomposed according to the reaction

cementite _____ ferrite + graphite.

Mechanistically stated, carbon leaves the cementite lattice and

¹ Research Associate, Massachusetts Institute of Technology, Cambridge, Mass.

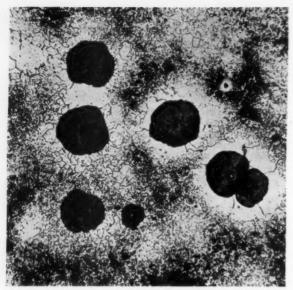


Fig. B-Pearlite in nodular cast iron partially decomposed by annealing 40 min at 720 C (1328 F). 300×.

diffuses through ferrite to a graphite nodule where it deposits; the iron atoms of the decomposing cementite and those of the ferrite of the pearlite form new massive columnar grains of ferrite radiating from the graphite nodule. (Fig. B).

If we can indicate the driving force of these reactions by the carbon concentrations involved, then their relative magnitudes

may be shown as in Fig. A.

Both reactions (2) and (3) appear to proceed in nodular iron without further nucleation of graphite. Since these reactions are diffusion controlled, an increase in number of effective nodules will decrease the diffusion distance and hence speed the reaction, as Mr. Rehder has noted. In both reactions it is not unusual to observe that the carbon deposits on the nodules as a normal extension of the spherulitic structure, as illustrated in Fig. 9 of the paper.

Mr. Rehder's conclusion 5 appears to stem from a comparison of reactions (2) and (3), although that is not made clear. The speed of reaction (2) has been measured as a function of temperature, and the reaction is found to proceed *faster* as the temperature is lowered, while the reverse is true for reaction (3).

A quantitative study of the mechanisms and kinetics of these and other graphitization reactions, made at the Carnegie Institute of Technology under a fellowship grant by the American Brake Shoe Company, is the subject of a forthcoming publication.

MR. REHDER (Reply to Mr. Brown): Mr. Brown's discussion of graphitization theory is appreciated, although the writer cannot agree that a clear case has been made. To state that the graphitization reaction may be written

at temperatures above the critical temperature range is not only stating the obvious, but begs the question. The principal unknown—how cementite decomposes or dissolves in the austenite, and what factors quantitatively affect the solution—is ignored. Furthermore, any description of the mechanism of decomposition of cementite in iron-carbon-silicon alloys must explain quantitatively the accelerating effect of silicon, on which considerable experimental data exists.

The description by Mr. Brown of the reactions during cooling through the critical temperature range is also considered inadequate, resting on statements that "the carbon atoms (from the austenite) diffuse through the ferrite to plate out on the nodules," and that the "carbon leaves the cementite lattice and diffuses through ferrite to a graphite nodule."

diffuses through ferrite to a graphite nodule."

It might be mentioned that the observation that rate of graphitization increases with increase in nodule count has in the literature frequently been treated superficially. In the first

place, graphitization rates are not increased sufficiently by a given increase in nodule count to admit explanation by simple decrease of diffusion distances. In the second place, no one has yet, to the writer's knowledge, demonstrated that increased nodule count is caused by increased graphitization rate, or the reverse, i.e. the relation of cause and effect is at present unknown.

C. K. Donoho's Discussion

C. K. Donoho (Written Discussion): This paper gives us a great deal of valuable information about the kinetics of the decomposition of primary carbides and pearlite in nodular iron. The particular annealing cycles arrived at, by our experience, are much longer than will normally be required for the usual nodular irons. Although the Mg analysis of 0.047 per cent does not indicate it, we suspect that the magnesium content is quite excessive in the cast with which Mr. Rehder experimented. Excess magnesium is a powerful carbide stabilizer causing retention of free carbides in the as-cast structure and also stabilizing both the free carbides and the pearlite against decomposition by heat treatment.

At the American Cast Iron Pipe Company we have many times annealed 1-in. x $1\frac{1}{2}$ -in. keel block coupons by holding for 1 hr at 1650 F and then for 1 hr at 1350 F. In most cases the matrix after such treatment was more than 90 per cent ferrite and the elongation over 20 per cent. In many casts where the matrix was largely ferrite in the as-cast, simply holding for 1 hr at 1350 F produced a completely ferritic matrix.

We have encountered some casts where the magnesium was excessive which behaved in a manner similar to the iron studied in this paper. We have also encountered the unidentified non-metallic inclusion illustrated, but only when the magnesium content was considered to be highly excessive. The annealing cycles suggested in this paper may be considered as approaching the maximum annealing times which will ever be required for nodular iron. We feel that most nodular iron castings will be satisfactorily annealed much more quickly.

MR. REHDER'S REPLY

MR. REHDER (Reply to Mr. Donoho): The discussion of Mr. Donoho is much appreciated, and the writer is in general agreement that commercial annealing cycles considerably shorter than those mentioned in the paper are adequate. The principal object of the paper was to point similarities in kinetics between nodular iron and malleable iron.

The writer is reasonably satisfied that the magnesium content shown is within 10 per cent of the true value, and that large excess does not exist. The facts that no post-inoculant was used, and that carbon contents were relatively low, probably account largely for the extended annealing times found. Induction vs cupola melting is also a factor.

It is agreed that magnesium acts as a carbide stabilizer in itself. However, the brownish-black inclusions described are powerful stabilizers of pearlite in themselves, or at least coincide in position with areas of carbide stabilization in the matrix, and can occur in irons of relatively low magnesium content. The formation and occurrence of these inclusions is described in a paper by the writer published in the September, 1950 issue of American Foundryman.

Further data on the impact strength of these irons may be inserted here. Using completely ferritic coupons, Charpy impact bars were machined. Half of these bars were given a typical galvanizing treatment, and all bars were then broken. Results are in each case averages of two specimens, and are presented in Table A.

TABLE A—Susceptibility to Galvanizing Embrittlement Charpy Impact, Ft-lb

| As | Annealed | Galvan | nized |
|-----------|----------|-----------|---------|
| Unnotched | Notched | Unnotched | Notched |
| 121 | 15.5 | 99 | 12.8 |

It will be seen that although there is apparently some susceptibility to galvanizing embrittlement, it is not serious.

² Chief Metallurgist, American Cast Iron Pipe Co., Birmingham, Ala.

RICHARD SCHNEIDEWIND: ⁵ This fine paper of Mr. Rehder's brings up three points on which I wish to comment. We have shown that there is a straight line relationship between the yield point and the logarithm of the per cent elongation in maleable and pearlitic malleable irons. If the same type of plot is made for Mr. Rehder's heat treated nodular irons, the results plot in a line parallel to these for malleable and pearlitic malleabe irons but at superior elongation levels. The comparison is further increased in favor of the annealed and heat treated nodular irons if account is taken of the fact that in measuring elongation, the gate length is standard for nodular irons and is a special gage length in the case of malleable.

A second point of comparison between annealed nodular iron and malleable is the fact that the slope of the line correlating annealing time and temperature as shown in the paper is different for first stage than for second stage. We noted the same

phenomenon in the case of malleable irons.

For the discussion of the third point, I am indebted to Max Kuniansky for the privilege of describing some results of studies on irons from his foundry. These irons were cast white in water-cooled metal molds; they were cast iron pipe sections. The compositions were: carbon, 3.78 to 3.90 per cent; silicon, 2.64 to 3.00 per cent. The manganese was deliberately set at 0.32, 0.66, and 0.81 per cent. The irons were treated with magnesium. First stage graphitization was completed in about 15 min at 1700 to 1750 F. It was desired to find a cycle which would produce maximum ferrite in the shortest time.

Furnace cooling to 1500 F and air cooling produced only 20 per cent ferrite in the sample containing 0.32 per cent Mn. Furnace cooling to 1500 F, holding at 1500 F for 15 min followed by air cooling gave the identical structure. Evidently 1500 F is above the lower limit of the 3-phase region, the "pearlitic interval." But furnace cooling from 1700 to 1500 F and cooling from 1500 to 1470 F in 12 min produced 90 to 160 per cent of

ferrite.

i

n

e

d

As the manganese content was increased, the formation of ferrite was slowed down markedly. With 0.66 per cent Mn, cooling from 1500 F to 1350 F in 120 min produced 90 per cent ferrite. With 0.81 per cent Mn, the decomposition of pearlite was still slower.

Upon heating a white iron to some temperature above the critical the austenitic matrix is saturated with carbon to a value corresponding to that temperature. If held at heat a short time an insignificant amount of nucleation of temper carbon may occur.

If the iron is now quenched, martensite is obtained which, however, has a higher concentration of carbon than the eutectoid. Upon reheating, when the iron has just exceeded the critical temperature a supersaturated austenite is produced which probably tends to nucleate temper carbon more readily than the austenite obtained when heating ordinary white iron castings which have not been quenched.

As a practical observation Mr. Boegehold of General Motors Research has been quoted as saying that a quenched white iron is so active that if you carry it past a stove it will automatically

graphitize.

MR. REHDER: It is encouraging that Prof. Schneidewind is able to corroborate some of these points because his work has been along similar lines. I might point out that with respect to the case of very rapid annealing which Prof. Schneidewind mentioned, the iron was a chilled iron. In such cases, the carbides are finely subdivided and can be decomposed at rates many times those possible with sand-cast irons. I think we agree however, that the mechanism and kinetics of graphitization involved are similar.

J. L. YARNE: 4 Mr. Rehder stated that the inoculant was added primarily to eliminate the carbides and I do not quite entirely agree with that. I have found that with identical metal conditions, castings poured from magnesium-treated metal which was not post-inoculated contained quasi-flakes, but under the same conditions if the metal was post-inoculated, the graphite was

entirely in nodular form.

MR. REHDER: I agree with you and will explain in this way. Magnesium ferrosilicon does not seem to be as much in need of post-inoculation for nodule formation, as are some of the other alloys. I have studied the effects of kind and amount of post-inoculant on nodule formation using nickel-magnesium and copper-magnesium alloys, and the effect is sufficiently important that in some cases the post-inoculant makes the difference between a partially and completely nodular structure.

³ Professor of Metallurgical Engineering, University of Michigan, Ann Arbor, Mich.

⁴ Research Metallurgist, Chain Belt Co., Milwaukee, Wis.

COMPOSITION AND PROPERTIES OF GRAY IRON

By

Richard Schneidewind* and R. G. McElwee**

ABSTRACT

The authors have attempted to reevaluate the well-known Maurer diagram and to reduce some of the relationships shown there to a more quantitative and readily usable basis. A mathematical expression has been derived and graphs are presented to show the limiting compositions with respect to carbon and silicon which will cast white. These values are restricted to normal cupola irons, unalloyed and uninoculated, over a range of section sizes cast in sand. A value is proposed to extend this to superheated air-furnace irons such as are used in the malleable industry.

Next, a correlation has been made between composition (as measured by the carbon equivalent) and the tensile strength and the section size for sand castings. Further a method is proposed to compute the probable strength of sand-cast gray irons of varying sections when alloyed.

It must be appreciated that any such methods for predicting the mechanical properties cannot be rigorous since it cannot take into consideration differences due to such factors as raw materials in the cupola, superheat, and pouring temperatures. Nevertheless, a statistical study made on over 300 heats of plain and alloyed irons shows that close to 75 per cent of the reported properties of these irons fall within \pm 10 per cent of the predicted value and between 85 and 90 per cent fall within \pm 15

per cent of the prediction.

The equation for predicting the probable tensile strength of gray irons cast in sand which is proposed is

$$T = 10,000 (11.68 - 2 \text{ C.E.} - 2.3 \log D_{\circ})$$

wher

$$T = tensile strength in psi$$

 $C.E. = carbon equivalent = \% C + \% Si + \% P$

 $D_{\bullet} \equiv equivalent \ diameter \ of \ the \ casting \ in \ inches$

When alloyed, the strength as computed above is multiplied successfully by factors, one factor for each alloy. These factors are presented in the paper.

For the purpose of extending the usefulness of this work a discussion of the relationship of tensile strength to other mechanical properties is included. The relationship with Brinell hardness is shown by the well-known MacKenzie equation.

In no case do the authors feel that the results of computations such as presented here can absolutely and accurately predict the mechanical properties. The probable maximum variation is in the order of \pm 20 per cent but an attempt has been made to propose values which are average and it is hoped that these may prove of value to the foundryman and to the designer.

IRON-CARBON-SILICON ALLOYS with appreciable quantities of silicon form the cast irons of industry: gray irons, white irons, and malleable cast irons. If the silicon contents are low and the carbon below 1.7 or 2.0 per cent the materials are usually classed as steels.

The iron-carbon-silicon eutectic may solidify from the liquid state in one of two forms under commercial rates of cooling: either as austenite and carbide or as austenite and graphite. At room temperatures, these will be pearlite and cementite and pearlite and graphite respectively. In the absence of alloying elements the mode of solidification into one or the other of the above mentioned forms or in a mixture of the two is determined by (a) the silicon content and (b) the rate of cooling from the molten state.

This fact has been recognized qualitatively for many centuries but serious quantitative work to investigate the influence of compositions and of cooling conditions on the structure was inadequate until Honda and Murakami¹ in 1923 and Maurer² in 1924 published their findings.

Figure 1 of Honda and Murakami show the structures obtained by casting 5 mm diam bars in metal molds. The areas contain the following constituents:

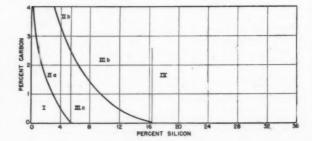


Fig. 1-Structure of Chill-Cast Iron-Carbon-Silicon Alloys (Honda and Murakami).

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I. $(Fe + Fe_3Si_2) + (Fe_3C) - ferrite$ and carbidesteel and white iron

IIa. (Fe + Fe₃Si₂) + (Fe₃C) + (Fe + Fe₃Si₂ + C) ferrite carbide, and martensite?

IIb. $(Fe + Fe_3Si_2) + (Fe_3C) + (Fe + Fe_3Si_2 + C) + (C) - ferrite, carbide, martensite?$

graphite-mottled iron, gray iron IIIa. (Fe + Fe₃Si₂) + (Fe + Fe₃Si₂ + C) - ferrite and martensite?

IIIb. (Fe + Fe₃Si₂) + (Fe + Fe₃Si₂ + C) + (C) - ferrite martensite? and graphite, gray iron

IV. (Fe + Fe₃Si₂) + (Fe₃Si₂) + (C) - ferrite, iron silicide, and graphite, gray iron

Note that in no case is cementite formed above 5.5 per cent Si. The commercial cast irons seldom contain over 3 per cent Si except certain special acid-resistant irons carrying around 12 to 14 per cent. In the commercial zones of composition the possible products are steel, white cast iron, mottled cast iron, and gray cast iron, the latter containing graphite and no free carbides.

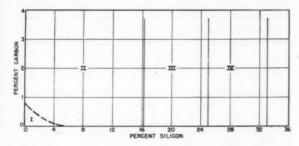


Fig. 2—Structure of Annealed Iron-Carbon-Silicon Alloys (Honda and Murakami).

Figure 2 from Honda and Murakami shows the structure after full annealing. The areas are as follows:

I. (Fe + Fe₃Si₂) + (Fe₃C) ferrite and carbidesteel

II. (Fe + Fe₃Si₂) + (C) ferrite and graphite malleable cast iron and gray cast iron

III. (Fe + Fe₃Si₂) + (Fe₃Si₂) + (C) ferrite, iron silicide, and graphite—acid resistant irons and silvery pig irons

IV. $(Fe_3Si_2) + (FeSi) + (C)$ two iron silicides and graphite

Honda and Murakami's contribution lies in showing the structures obtainable under very fast cooling rates and at equilibrium with variations in carbon up to 4.0 per cent and in silicon up to 36 per cent.

In 1924 Maurer² published the first of his diagrams shown in Fig. 3, in which he presented what he found to be the structures of irons poured in sand. The areas are as follows:

IIa. Pearlite, cementite and graphite...Mottled iron II. Pearlite and graphite......Pearlitic gray iron

II. Pearlite and graphite......Pearlite gray iron III. Pearlite, ferrite, and graphite......Ferritic gray iron III. Ferrite and graphite......Ferritic gray iron

Many similar diagrams were published by Maurer and his associates for various casting conditions since

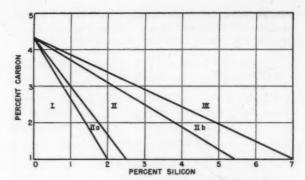


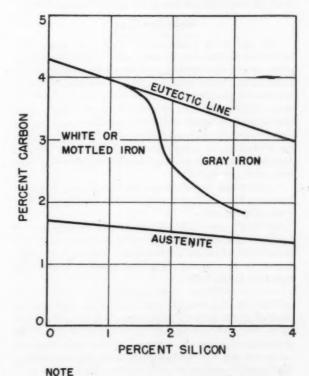
Fig. 3-Field Diagram of Microstructures of Sand Cast Iron-Carbon-Silicon Alloys (Maurer).

it was soon recognized that the original diagram was not universal in its application. Coyle³ attempted to superimpose the tensile strength data upon the Maurer diagram.

Aside from some obvious shortcomings of the Maurer diagram, the principal fault with the earlier investigators was the fact that the matter of cooling rate or section size was not adequately appreciated.

Schneidewind and McElwee⁴ in 1939 pointed out from theoretical considerations and as a result of experiment that the limit line between the white and mottled areas was not a straight line and did not intersect the point, %C = 4.3 and %Si = 0 as shown in Fig. 4.

Uhlitzsch and Weichelt⁵ carefully repeated the



0.875 BARS WITH 0.3 % VANADIUM

Fig. 4-Gray to White and Mottled Limit Line.

Maurer work with bars varying from 6 mm to 30 mm in diam (0.236 in. to 1.18 in.). These diagrams shown here as Fig. 5 have been well worth study.

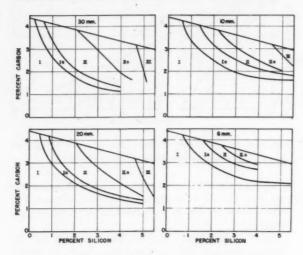


Fig. 5-Structure Diagrams of Sand Cast Irons (Uhlitzsch and Weichelt).

The white iron boundary line is curved and tends to become asymptotic at a constant per cent carbon value; this value increases with increasing cooling rate as might be expected. It is obvious that mottled and gray irons cannot be formed if the carbon content is below the maximum solubility in austenite or in other words if no eutectic exists at the solidus temperature.

White Iron Zone

The operating foundryman wishes to know the maximum carbon and silicon contents for a casting of given section size poured in sand which will result in a completely white structure. The limit lines of Fig. 5 purport to predict this for uninoculated crucible furnace irons cast in sand. The values seem to agree with normal cupola iron results. Superheat in an air or electric furnace such as done in the malleable industry or melting in a high frequency induction furnace would probably permit slightly higher silicon contents than shown here.

Figure 5 was replotted on semilogarithmic paper as shown in Fig. 6. It will be seen that the lines of Fig. 5 become straight.

An effort was made to extend the information of Uhlitzsch and Weichelt to a wider range of useful sections. The white to mottled limit lines of Fig. 6 are found to be four straight and parallel lines. The equation of the lines is

 $%C = -2.85 \log \%Si + b$

The value of b varies with section size and may be read from the plot directly where %Si = 1 (log %Si = 0). By plotting b against log D where D equals the diameter in inches, the influence of rate of cooling due to section size can be computed. This plot results in a line whose equation is

 $b = -1.6 \log D + 2.703$

The complete equation now reads $C = -2.85 \log Si - 1.6 \log D + 2.703$

Since all castings are not round bars, computations for other shapes will have to be made indirectly. Cooling rates of quenched bars of steel and of castings have been associated with the ratio $\frac{V}{A}$, the volume in cubic

inches divided by the surface area in square inches. For example, a square bar 1 in. by 1 in. and 10 in. long would have a

 $\frac{V}{A} = \frac{1 \times 1 \times 10}{10 (4 \times 1) + 2 (1 \times 1)} = \frac{10}{42} = 0.238 \text{ in.}$

If a gate 1 in. square is used, then an area 1 in. x 1 in. must be subtracted for obviously no cooling is done through the gate and V becomes 10 = 0.244 in.

 \overline{A} $\overline{41}$

If the bar is very long, the areas of the ends can be neglected and V for a 1-in. bar would be 0.25 in. In

other words for a long square or round bar $\frac{V}{A} = \frac{D}{4}$.

The ratio V can similarly be computed for other \overline{A}

shapes. Since, however, the foundryman thinks in terms of round test bars whose cooling rates should be comparable to those of various castings he makes it may be inconvenient to leave the computations as V

but can be converted to an equivalent round bar

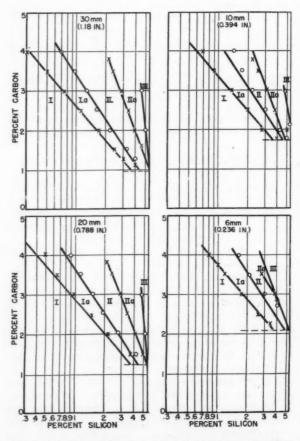


Fig. 6-Structure Diagrams of Sand Cast Irons. Semilog Plot. (Uhlitzsch and Weichelt).

cooling rate. For simple shapes

V = D for long bars, round or square $\overline{A} = D$ for plate sections where D = thickness $\overline{2}$ $\overline{2}$ $\overline{6}$ $\overline{6}$

Suppose a casting is to be made 0.75 in. thick, 4 in. wide, and 10 in. long and the area of the gate and riser at the points of attachment is 2 sq in.

V = (0.75) (4) (10) = 30 cu in. A = (10) (2) (4.75) + (2) (0.75) (4) = 95 + 6 = 101 sq in. minus the gate and riser area = 99 sq in.

 $\frac{V}{A} = \frac{30}{99} = 0.303 \text{ in.}$

A bar having the same cooling rate could have the same V of 0.303 in. Its diameter may be termed the

"equivalent diameter" and would have a $D_e=4\times 0.303=1.212$ in. since $V=D \over A$ for long bars. A

standard B bar would be approximately equivalent to the above casting.

Figure 7 has been plotted with a series of lines indicating the carbon-silicon relationships for bars of various diameters which if poured in sand of uninoculated iron will produce white iron.

For greater ease in reading, Fig. 8 has been plotted with log %Si as the ordinate and log D_e (equivalent diameter) as abcissa. The diagonal lines are for constant carbon compositions. For example, if an iron contains 3.25 per cent C and 1.8 per cent Si, the largest sand casting to give a clear white fracture will have an equivalent diameter of 0.16 in. A plate could be cast all white 0.08 in. thick.

It is pointed out again that inoculation will reduce the D_e for white iron of a given composition.

On the other hand, holding the iron in an air furnace will increase the $D_{\rm e}$. For example, let %C=2.25 per cent and %Si=1.3 per cent. The largest round bar section to cast white in sand reads 1.2 in. in diam. For superheated, slightly oxidized irons in the malleable industry it is estimated that the above com-

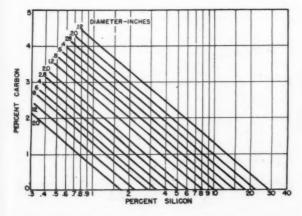


Fig. 7-Carbon-Silicon Limits for Sand-Cast White Irons.

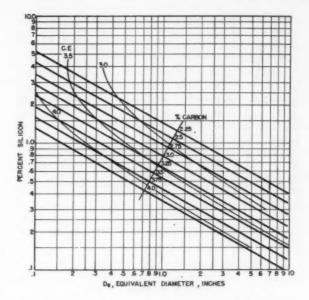


Fig. 8—Composition and Section Limits for Sand-Cast White Irons.

position could cast white in about 1.5-in. section. Expressed another way for a maximum diameter of 1.1 in. the silicon could be raised another 0.2 per cent or expressed still another way, the last number in the equation is raised from 2.703 to 2.8 or 2.9.

In Fig. 8, carbon equivalent lines superimposed show that the carbon equivalent is not too valuable in predicting chill. This confirms MacKenzie's observation.

Mottled Iron Zone

The mottled iron zone as shown by Honda,¹ Maurer,² and others is the transition from clear white iron to completely pearlitic gray iron. Though still troublesome to foundrymen, its control is now much better understood than in the past. By proper selection of raw materials, superheat, pouring temperature and other foundry conditions the white-mottled boundary line can be shifted to the right, the higher silicon direction. By control of the above factors in the other direction or by intelligent inoculation programs it may be shifted to the left, the lower silicon direction.

The boundary line between mottled and pearlitic irons is likewise a vague area at the present time. Proper inoculation will prevent carbide precipitation at silicon contents below those without inoculation. The inoculant will also tend to counteract the chilling tendency of carbide-forming alloys.

Schneidewind and Hoenicke⁶ showed that the transition of mottled to gray irons is frequently associated with a zone of eutectiform graphite, ASTM type D, and a matrix which may be completely ferritic. A hardness traverse on a section of a wedge from the tip to the butt shows a hard zone, a zone of diminishing hardness, a zone of minimum hardness, a zone of increasing hardness, and a zone of slowly diminishing hardness. This is presented in Fig. 9. The hard zone is white iron; as the hardness decreases the amount of mottle increases. The minimum hardness zone con-

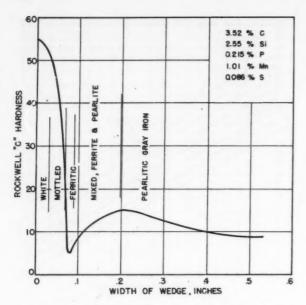


Fig. 9-Metallographic Structure Zones of Cast Irons (Schneidewind and Hoenicke).

tains a large quantity of "primary" ferrite; in the zone of increasing hardness, the amount of ferrite diminishes and the eutectiform graphite changes to a dendritic orientation, Type E. The last zone is the most important to the foundryman; the graphite is the random flake, Type A, and the matrix is pearlite. As the section increases the flakes grow larger and the pearlite coarser. At very large sections the pearlite tends to decompose below the critical temperature like second stage graphitization in malleable iron and ferrite is thus produced.

This transition of graphite shape with cooling rate was demonstrated by D'Amico and Schneidewind7 and the shape of the hardness traverse on wedge castings was also shown by Piwowarsky.8 The latter stated that the minimum point was most frequently found in high silicon irons. Hence, it may be inferred that high silicon irons have the greatest tendency to produce

eutectiform graphite.

Since the mottled zone is indefinite, attention will be directed rather to the pearlitic zone which is the best zone for the gray iron foundryman.

Pearlitic Gray Iron Zone

The stronger grades of gray cast irons today contain pearlite and flake graphite as their principal constituents. Cementite is to be avoided because of increased brittleness and difficulty of machining. Ferrite is accompanied by loss in strength and wear resistance. An iron with only pearlite and flake graphite may be termed a "balanced" gray iron and in the unalloyed state has a tensile strength between 30,000 and 45,000 psi, sometimes ranging as high as 50,000 psi.

The Carbon Equivalent

Many investigators 10,11,12,13,14 including the present authors4 have shown that for sand cast bars, the same mechanical properties can be secured of widely different chemical compositions. This is true if the bars have the same "carbon equivalent" which is a function of the composition.

When gray iron solidifies and cools the product is graphite and pearlite (and ferrite). It derives its strength from the pearlite (and ferrite) since the graphite is weak. Analysis for per cent of graphite will not in itself explain the properties since the cooling rate and other factors may alter the size, shape. and distribution of the graphite and may also alter the nature of the pearlitic matrix.

Most of the graphite is produced on solidification of the eutectic. Hence a measure of how much eutectic exists in a given iron is helpful since the more eutectic, the more graphite is formed and the lower will be

the strength and hardness.

If no silicon or phosphorus or other such element is present, the metal would be 100 per cent eutectic if the carbon is 4.3 per cent. The presence of other elements will replace some of the carbon so that less than 4.9 per cent will be required to produce 100 per cent eutectic. In other words to have 100 per cent eutectic requires carbon, silicon, and phosphorus which in certain proportions are equivalent to 4.3 per cent.

Amount of silicon required to replace 1 per cent of carbon has been given as 3.00, 3.21, and 3.33 per cent silicon by various investigators. 15,11,4 Differences in results are less than the normal variations in commercial work. Hence in the absence of phosphorus, in the 100 per cent eutectic composition %C + %Si = 4.3% in this paper.

The amount of phosphorus to replace 1 per cent of carbon has been given as 3.00 to 3.33 per cent of phosphorus. 15,12 Hence in the 100 per cent eutectic composition C + Si + P = 4.3 or C + 0.3 (Si + P) 3 = 4.3.

Examination of the results of various investigators leaves some doubt as to the correctness of the above phosphorus factors. This phosphorus correction was proposed by MacKenzie¹⁵ and the work of Peace and Russell¹⁶ seems to confirm it. Figure 10 presents the results of a series of investigators on the influence of phosphorus on the carbon-iron eutectic. Wüst, Goerens and Dobbelstein¹⁷ worked out an Fe - C - P ternary diagram with a triple eutectic at 7 per cent P and 2

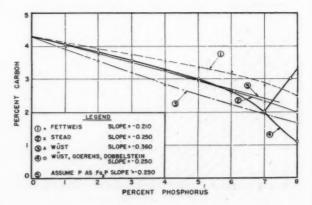


Fig. 10-Effect of Phosphorus on the Iron-Carbon

per cent C. Up to 5 per cent P their eutectic is confirmed by the results of Stead¹⁸ who indicates a 4 to 1 relationship of phosphorus to carbon in the eutectic.

If a computation is made in which it is assumed that all the phosphorus exists as Fe_3P and that the remaining iron and carbon independently form a eutectic with 4.3 per cent carbon, the results check those of the above investigators closely. This method of computation has been successfully used by Rehder¹⁹ in approximating the eutectic composition in the Fe-C—Si system although it may have no rigorously scientific basis.

In Fig. 10 two other sets of results are also given; one shows about a 3 to 1 phosphorus to carbon relationship and the other almost a 5 to 1. On the basis of the results in Fig. 10 and also the mechanical properties of Von Kerpely's²⁰ high phosphorus irons to be discussed shortly, the carbon equivalent used in this paper will be

$$C + \frac{Si}{3} + \frac{P}{4} = 4.3$$
 per cent

at the eutectic composition. American irons carry such low phosphorus contents that any of the equations proposed will be satisfactory. Since some European irons carry 1 to 2 per cent phosphorus it is felt the equation of this paper is preferable.

In a commercial cast iron the value $C + \frac{Si}{3} + \frac{P}{4}$

usually does not equal 4.3 which corresponds to 100 per cent eutectic. The value is apt to equal a number less than 4.3 per cent showing that the iron at solidification contains only a fraction of eutectic. Since the amount of eutectic is a measure of the quantity of graphite formed (a weak constituent) it is also a measure of the strength. If $C + \frac{Si}{3} + \frac{P}{4}$, the carbon

equivalent, is high the iron is weak; if low, the iron is stronger.

Tensile Strength and Carbon Equivalent

Carbon equivalent is an approximate measure of per cent eutectic and is fairly accurate if commercial irons only are considered with carbon varying from 2.8 to 3.8 per cent and silicon from 1 to 3 per cent.

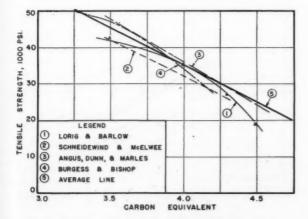


Fig. 11—Tensile Strength of 1.2-in. Diam Bars as Affected by the Carbon Equivalent.

Perhaps the most ambitious and complete work to correlate strength and carbon equivalent has been done by Angus, Dunn, and Marles. Figure 11 shows the averaged results of these investigators and some others. It may be seen that there is some disagreement but Angus, Dunn, and Marles pointed out that the scatter at any value of carbon equivalent amounted to \pm 3000 psi.

A line was selected by the present authors with previous work as a guide and from data to be presented here. The equation of this line is T=11.5-2 C.E. 10.000

where T = tensile strength in pounds per square inch and C.E. = carbon equivalent.

Example 1: Let C = 3.25%, Si = 2.25%, P = 0.16%, standard B bar cast in sand.

C.E. =
$$3.25 + \frac{2.25}{3} + \frac{0.16}{4} = 4.04$$

T = $10,000 (11.5 - 8.08) = 34,200 \text{ psi.}$
Angus, Dunn, and Marles:
C.E. = $3.25 + \frac{2.25 + .16}{3} = 4.083$
T = $34,000 \text{ psi.}$
Lorig and Barlow:
C.E. = 4.083
T = $34,000 \text{ psi.}$

Example 2: Let C = 3.8%, Si = 2.7%, P = 0.40%, a typical automotive piston ring iron, casting is a standard B bar cast in sand.

T =
$$(11.5 - 9.6)$$
 $10,000 = 19,000$ psi.
C.E. = $3.8 + \frac{2.7}{3} + \frac{0.4}{4} = 4.8$
Experimental T = $18 - 20,000$ psi.

Limitations in Predictions

In any chart of strength vs C.E. the upper limit of accuracy will be at about 45,000 psi in uninoculated irons. Due to the possibility of free carbides or of ferrite and eutectiform graphite some of the experimental points will tend to be lower than predicted by a straight line relationship. Where the carbon equivalent is low and the iron is inoculated, a straight line relationship between strength and carbon equivalent is good up to slightly about 50,000 psi.

At very high carbon equivalents, especially with high silicon contents, the strength values sometimes are on the low side. This probably occurs due to second stage graphitization giving more ferrite than expected when the castings are not shaken until cold.

The line T = 10,000 (11.5 – 2 C.E.) does not purport to predict exactly the strength but gives an average prediction. Results from some foundries consistently plot 10 to 15 per cent high in strength; other foundries results may be consistently on the low side. This is due to differences in raw material, superheat, pouring temperature, sand, and general foundry practices.

Phosphorus and the Carbon Equivalent

If it is accepted that strength roughly is a function of the carbon equivalent, the work of von Kerpely²⁰ makes an interesting study to correlate the influence

Table 1— Effect of Phosphorus on Tensile Strength

| %C | %Si | %P | C.E.ª | C.E.b | Experimental T.S. |
|----------|---------------------|-----------------|-------|-------|----------------------------------|
| 3.10 | 1.78 | 0.026 | 3.702 | 3.701 | 32900 |
| 2.94 | 1.66 | 0.123 | 3.534 | 3.524 | 37000 |
| 3.03 | 1.75 | 0.222 | 3.687 | 3.668 | 40200 |
| 2.87 | 1.77 | 0.308 | 3.563 | 3.537 | 44900 |
| 3.00 | 1.74 | 0.504 | 3.748 | 3.706 | 39700 |
| 3.00 | 1.57 | 0.670 | 3.747 | 3.691 | 33500 |
| 2.85 | 1.68 | 1.806 | 4.012 | 3.886 | 33400 |
| 2.85 | 1.81 | 2.775 | 4.378 | 4.147 | 27800 |
| 2.71 | 1.63 | 3.050 | 4.270 | 4.015 | 25100 |
| 2.88 | 1.57 | 4.173 | 4.794 | 4.446 | 24900 |
| 2.95 | 1.63 | 5.095 | 5.192 | 4.768 | 20400 |
| (a) CE : | $= c + \frac{1}{2}$ | $\frac{i+P}{3}$ | (b) | CE = | $C + \frac{Si}{3} + \frac{P}{4}$ |

of phosphorus. Table 1 shows the compositions and tensile properties of a series of irons whose phosphorus contents vary up to over 5 per cent.

If the carbon equivalent is computed as $C + \frac{Si + P}{9}$

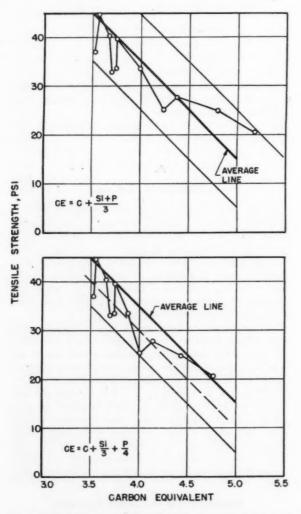


Fig. 12-Strength of High Phosphorus Irons Plotted Against Two Different Carbon Equivalents (von Kerpely).

the tensile values at high phosphorus concentrations do not parallel the trend found in all other irons, see Fig. 12. On the other hand, if the carbon equivalent is computed $C + \frac{Si}{3} + \frac{P}{4}$ the agreement is much better.

The overall tensile values of von Kerpely's irons seem about 5000 psi lower than expected. This circumstance may be explained by difference in raw material and lower superheat and pouring temperature than is common in present day iron.

Tensile Strength and Section Size

Angus, Dunn, and Marles, 12 Schneidewind and Mc-Elwee⁴ and others have presented data showing lines similar to those of Fig. 11 for bars cast in different sizes. The precise location and slopes of the lines found by different investigators do not agree exactly. It was felt that reasonable and consistent results would be obtained if the lines were considered parallel to one another.

The next problem was to determine the spacing between these lines for various section sizes.

Many investigators have reported the tensile strength of irons when poured into bars or castings of different sections. Jungbluth and Heller²¹ presented data and indicated that strength was an exponential function of the section. In other words, a log-log relationship exists between tension and diameter.

The data investigated were taken from many sources (Ref. 4, 12, 21, 22, 23, 24, 25, 26, 27, 28).

Only a portion of the data can be presented here. Table 2 gives in tabular form the results of some of these investigators. Various methods of correlation were tried but only two show promise of accurately predicting the experimental data.

One method was to plot T, the tensile strength in psi against $log\ D$ where D is the diameter of the bar in inches. The other is to plot $log\ T$ versus $log\ D$. Figure 13 presents data of Mahin and Lownie,²² Winte,²⁴ Angus, Dunn and Marles,¹² Jungbluth and Heller,²¹ and MacCready²⁹ who described properties of automotive piston ring iron. The experimental points were plotted and a common slope was selected such that when lines of this slope were passed through the ex-

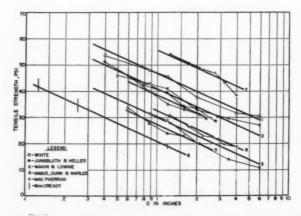


Fig. 13-Tensile Strength as Influenced by Section Size, Cast in Sand (Semilog Plot).

S

TABLE 9_TENSILE TEST VALVES

| | Jungblutl | and Heller | |
|----------|-----------------|------------------------|---------|
| Diam, mm | Diam, in. | TS, Kg/mm ³ | TS, psi |
| 10 | 0.394 | 36 | 51,200 |
| 20 | 0.788 | 30 | 42,600 |
| 30 | 1.180 | 27.7 | 39,400 |
| 40 | 1.576 | 24 | 34,200 |
| 50 | 1.970 | 23.8 | 33,800 |
| 70 | 2.758 | 20 | 28,400 |
| Grav | Iron Inst. 36 " | CAST METALS HAD | NDBOOK |
| | | outed from Ratio | |

Computed from Experimental Results

| T | | inputed if | om Exper | mental F | csuits | |
|-------|---------|------------|---------------------|----------|------------|-------|
| Diam. | | CDC: | TCC: | me: | TOC | me |
| in. | TS, psi | TS, psi | TS, psi | TS, psi | TS, psi | TS, p |
| 0.75 | 20,000 | 25,000 | 30,000 | 35,000 | 40,000 | 50,00 |
| 1.10 | 15,800 | 20,300 | 23,400 | 29,000 | 34,000 | 45,00 |
| 1.60 | 12,000 | 18,250 | 21,300 | 23,800 | 32,000 | 39,50 |
| 2.00 | 10,600 | 18,000 | 20,100 | 23,100 | 26,800 | 36,00 |
| | TO: . | | MacPherra | n | FEC | |
| | Diam, i | in. | TS, Psi | | TS, psi | |
| | 1.25 | | 31,000 | | 54,000 | |
| | 1.75 | | 26,610 | | 50,750 | |
| | 2.25 | | 22,950 | | 48,050 | |
| | 2.75 | | 22,000 | | 46,950 | |
| | 3.25 | | 19,600 43,200 | | | |
| | 4.00 | | 18,900 | | 38,350 | |
| | | Angus, | Dunn and | | | |
| | | | C.E. = 4 | 0 (| C.E. = 4.5 | |
| | Diam, i | in. | TS, psi | | TS, psi | |
| | 0.6 | | 46,000 | | 34,000 | |
| | 0.875 | | 40,000 | | 28,000 | |
| | 1.2 | | 35,000 | | 24,000 | |
| | 1.6 | | 33,000 | | 23,000 | |
| | 2.1 | | 31,000 | | 20,000 | |
| | | Mal | nin and Lo | wniess | | |
| | Diam, i | n. | TS, psi | | TS, psi | |
| | 0.5 | | 37,000 | | 46,000 | |
| | 0.875 | | 31,500 | | 43,000 | |
| | 1.2 | | 29,000 | | 41,000 | |
| | 2.0 | | 20,500 | | 33,000 | |
| | 3.5 | | 14,000 | | 29,500 | |
| | 6.0 | | 11,000 | | 27,000 | |
| | | | Winte ⁸⁴ | | | |
| | D | iam, in. | | TS, p | si | |
| | | 0.4 | | 53,70 | 00 | |
| | | 0.875 | | 48,00 | 00 | |
| | | 1.2 | | 45,00 | 0 | |
| | | 2.0 | | 37,70 | | |
| | | 4.0 | - | 31,40 | 0 | |
| | | 6.0 | | 30,40 | 0 | |

perimental points the least deviation was listed.

Line I through the points of Winte show a maximum deviation of 2300 psi and an average deviation of + 517 psi as drawn.

Line 2 serves for the points of Jungbluth and Heller and also for the higher strength iron of Mahin and Lownie. The deviations of the experimental points from line 2 as drawn is less than ± 2000 psi with the exception of Mahin and Lownie value for a 6-in. diam bar.

Line 3 drawn through the points of the Mahin and Lownie's weaker iron show a maximum deviation of 3400 psi for the 3.5-in. diam bar but all other points deviate less than ± 1000 psi.

Lines 4 and 5 were drawn through values obtained by Angus, Dunn and Marles. Line 4 shows a deviation of -2300 psi for the 0.6-in. diam bar, all other points fall less than 1800 psi from the line. In line 5 all points fall less than 1500 psi from the line.

Line 6 was drawn through three points on production irons reported by MacCready. The iron contained 3.8 per cent C, 2.7 per cent Si, 0.4 per cent P. Standard piston rings of 1/8 in. x 1/8 in. section show tensile strengths between 40,000 and 45,000 psi. The same iron poured into a "D" ring of 1/4 in. x 1/4 in. section show tensile strengths between 32,000 and 37,000 psi. A 1.2-in. diam test bar section of this iron has a tensile strength between 18,000 and 20,000 psi. A line of the same slope as the others satisfactorily goes through all points. Lines 7 and 8 were drawn for MacPherran's results.

It can readily be seen that a line of the slope selected reasonably correlates tensile strength with section size. The equation of this line will be

 $T = 10,000 (b' - 2.3 log D_e)$ where T = tensile strength in psi

D_e = diameter or equivalent diameter in inches.

b' = a constant.

Correlation of Strength, Carbon Equivalent, and Section

It was shown previously that for a 1.2-in. diam section tensile strength could be computed from

$$\frac{T}{10,000} = 11.5 - 2 \text{ C.E.}$$

where T = tensile strength in psi and C.E. = carbon

Assume that a given iron has a carbon equivalent of 4.0. In the above equation T = 35,000 psi; substituting this value in the previous equation

$$35,000 = b' - 2.3 \log. 1.2$$

and b' is found to be 3.68. Then for this iron having a carbon equivalent of 4.0 the tensile strength of bars of other section diameters can be computed.

 $= 3.68 - 2.3 \log D_e$ the For example, if T 10,000

tensile strength for some other diameters will be as follows:

| D. | T | b' |
|-------|--------|-------|
| 0.10 | 59,800 | 13.98 |
| 1.00 | 36,800 | 11.68 |
| 1.20 | 35,000 | 11.50 |
| 10.00 | 13,800 | 9.38 |

But to simplify computations for the foundryman one more step is made. Since we have computed the tensile strength for these sections, the values can be substituted in the original equation T = b - 210,000

C.E. and we find that the constant, b, which was 11.5 for a 1.2-in. diam bar, is a different value as the section size is changed as shown above. In Fig. 14, the values for b have been plotted against the section diameter, De, on a log scale. The equation of the line is as follows

$$b = 11.68 - 2.3 \log D_e$$

Then the complete equation correlating tensile strength, carbon equivalent, and section may be writ-

$$T = 10,000 (11.68 - 2 \text{ C.E.} - 2.3 \log D_e)$$

But the simplest manner to use this relationship is to consult Fig. 14 for the value of b corresponding to the section desired. From this number subtract twice the carbon equivalent. The result multiplied by

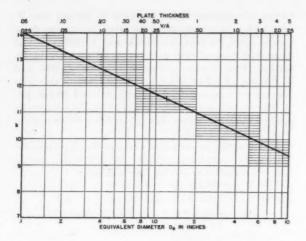


Fig. 14—Tensile Strength Constant, b, for Gray Irons to use: Find b for the desired section diameter, subtract from this twice the carbon equivalent and multiply by 10,000 to obtain the tensile strength in psi. $T=10,000\ (b-2\ C.E.)$

10,000 gives the tensile strength.

For example: C = 3.8 per cent Si = 2.7 per cent P = 0.4 per cent C.E. = 4.8 per cent

An automobile piston ring has a section 0.125 in. square which has about the same cooling rate as a 0.125-in. diam bar. From the chart for $D_{\rm e} \equiv 0.125$ in., $b \equiv 13.88$. Hence

$$13.88 - 2(4.8) = 13.88 - 9.6 = 4.28$$

 $4.28 \times 10,000 = 42,800 \text{ psi}$

Production figures²⁹ show that this ring pulls between 40,000 and 45,000 psi consistently.

Example 2: A casting is to be made 0.75 in. thick, 4 in. wide, and 10 in. long. The combined areas of gate and riser at the point of attachment is 2 sq in. It is desired to produce a gray iron of 38,000 psi. What composition should be used?

As shown before V = 0.0303 in.

$$\begin{array}{c} \overline{A} \\ D_e = (4) \; (0.303 \; \mathrm{in.}) = 1.212 \; \mathrm{in.} \\ \text{From the chart for } D_e = 1.212, \; b = 11.49 \\ T = 10,000 \; (11.49 \; -- \; 2 \; \text{C.E.}) \\ C.E. = \frac{7.99}{2} = 3.995 \end{array}$$

A wide variety of compositions may be used to produce this carbon equivalent—3.4 C 0.16 P 1.65 Si or 3.0 C 0.20 P 2.82 Si

For fluidity and castability the higher carbon iron would be preferable.

Figure 15 has been prepared for quick reference to correlate tensile strength with section size and carbon equivalent.

Second Method of Correlating Strength and Section Size

In order to investigate the feasibility of applying Jungbluth and Heller's relationship, Fig. 16 was drawn with log T as ordinate and log D as abcissa. After plotting the experimental data, it was found that fairly straight lines were obtained but the slopes were

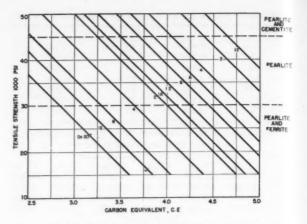


Fig. 15—Tensile Strength for Various Sections and Carbon Equivalents.

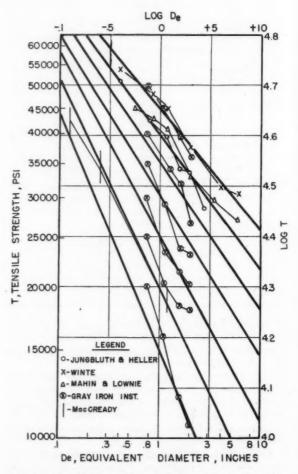


Fig. 16—Tensile Strength as Influenced by Section Size, Cast in Sand (Log-log plot).

flatter for strong irons and steeper for weak irons.

It was found that the slope was proportional to the strength as follows: $M = -0.5 \log T + b$ where M = slope, T = tensile strength and b = a constant. By using this value for the slope the final equation becomes

$$\log T = (-0.5) \left(\log \frac{T_1}{135000} \log \frac{D_1}{D} \right) + \log T_1$$

T = tensile strength to be computed for an iron of diameter, D.

 $T_1 =$ known tensile strength of the same iron poured in a bar of diameter, D_1 .

If $D_1 = 1.2$ in. then $T_1 = 10,000$ (11.5 – 2 C.E.) It would seem, therefore, that the semilog and the

log-log plots are equally useful within the limits found in commercial irons. Figure 17 shows how closely the results of the two methods of computation agree. Until new and more accurate experimental data are available no choice between the two can be made.

In this paper further discussion will be on the basis of the first method because it is simpler.

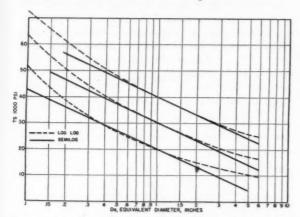


Fig. 17-Comparison of Results Obtained by Using the Semilog and Log-log Plots.

Further Control of Strength

Although a wide variety of sections, strengths and hardnesses may be produced from ordinary irons, frequently a specification demands higher strength than easily obtainable. Sometimes the iron is not easily cast in the size and shape desired with the composition chosen for strength.

It is obvious that for higher strengths a lower carbon equivalent is demanded. For the sake of castability and minimum shrinkage it is desirable to choose the higher carbon and lower silicon. In using a low carbon equivalent, chilled edges may be encountered or eutectiform graphite and ferrite. A good inoculant will assist in rectifying these difficulties. In a patented procedure for production of gray iron, member foundries have for years successfully followed the policy of choosing a low silicon content along with a carbon content suitable for castability. Such a composition, even assuming good raw material and foundry practice, would tend to cast mottled at the corners of the casting. Inoculation produced a pearlitic iron of low carbon equivalent and good properties.

Where the normal compositions fail to yield adequate properties, alloying is resorted to. Alloying elements may function in three ways:

(a) They may influence the nature of the eutectic solidification and minimize chill and eutectiform graphite. In this role the alloys serve as deoxidizers or "inoculants."

(b) They may affect the stability of the carbide either to decrease the tendency to form eutectic cementite or to increase it.

(c) They alter the transformation rate of austenite at the critical temperature and hence serve to produce stronger pearlites or to yield acicular structures.

In order to secure the best results from alloy additions, the base iron should be of good quality. The amount of alloy used will then be a measure of the increase in properties.

Much work³⁰ has been done on alloying of gray irons but the results have not been well systematized so that the foundryman who uses alloys does so as a result of his own experience. The effect of a given quantity of a given alloy is not yet too well known. In many cases, alloys are mixed so that for example the chill promoting tendency of chromium is counteracted by a chill reducing tendency of nickel and the two alloys, combined, affect the strength of the resulting pearlite matrix.

The following table is proposed as a working basis for alloy effect upon uninoculated cast iron of common 3.30 T.C., 2.0 Si type.

| | , | % Increa | |
|-------|---------------|----------|------------------------------------|
| Alloy | Max % used | for each | 1% Remarks |
| Mo | 1.00 | 40 | Chilling tendency mild |
| Cr | 0.50 | 20 | Chilling tendency strong |
| V | 0.35 | 45 | Chilling tendency very strong |
| Ni | 3.00 | 10 | Chilling tendency weak or negative |
| Cu | 1.50 | 10 | Chilling tendency weak or negative |
| Mn | _ | 10 | Chilling tendency weak |

The manganese to be considered is the amount above that usually found in normal irons or the manganese above 0.7 per cent.

For example, the first iron in "Specific Applications of Alloy Cast Iron," p. 200 of the Alloy Cast Iron HANDBOOK is for an automotive brake drum.

| Si | 2.20-2.30 | avg. 2.25 | |
|---------------|-----------------------------------|-----------|----------------------|
| C | 3.10-340 | avg. 3.25 | |
| P | 0.11 - 0.23 | avg. 0.17 | T.S. in 1.2-in. bar |
| Ni | 0.65 - 0.85 | avg. 0.75 | 46,000 to 50,000 psi |
| Mo | 0.65 - 0.85 | avg. 0.75 | average = 48,000 psi |
| C.E. = 3.25 + | $\frac{2.25}{3} + \frac{0.17}{4}$ | = 4.04 | |

Base strength = 10,000 (11.5 - 8.08) = 34,200 psiNi effect $= 0.75 \times 0.05 = .0375$ or 3.75% increase $34,200 \times 1.0375 = 35,400 \text{ psi}$ Mo effect \pm 0.75 \times 0.45 \pm 0.3375 or 33.75% increase $35,400 \times 1.3375 = 47,450$ psi computed

Second example, p. 202, ALLOY CAST IRON HANDBOOK Automotive and Diesel Crankshafts

| Si | 2.4-2.8 a | g. 2.6 T.S. in 2-in, bar |
|----------|-----------------|--|
| C | 2.4-2.9 | 2.65 60-75,000 psi |
| P | 0.05-0.08 | 0.065 avg. 67,500 psi |
| Mn | 0.8-1.0 | 0.9 |
| Ni | 1.0-1.25 | 1.12 |
| Mo | 1.0-1.25 | 1.12 |
| Cr | 0.1-0.2 | 0.15 |
| C.E. = | 2.65 + 0.86 + 0 | 016 = 3.526 |
| | | 7.052) 10,000 =39,480 psi |
| | Mn effect | $0.2 \times 0.1 = 0.02$ |
| | Ni effect | $1.12 \times 0.05 = 0.056$ |
| | Mo effect | $1.12 \times 0.45 = 0.505$ |
| | Cr effect | $0.15 \times 0.40 = 0.06$ |
| Strength | = (39,480) (1.0 | 2) (1.056) (1.505) (1.06) = 67,500 psi computed |

^{*} The computations and Fig. 18 are based on slightly different strength factors. These are: Mo 45, Cr 40, V 50, Ni 5, Cu 8, Mn 10.

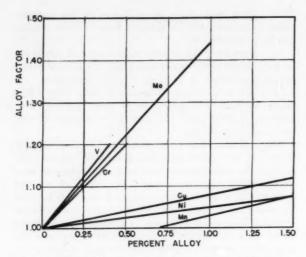


Fig. 18-Alloy Factors for Computing Tensile Strength of Gray Irons.

Figure 18 has been prepared in order that the alloy factors can more readily be read off. For a given amount of a given alloy read off the corresponding alloy factor and insert in the equation

T = 10,000 (b 2 C.E.) (f_1) (f_2) (f_3) (etc.)

where T = tensile strength in psi

b = a constant corresponding to the section size

C.E. = carbon equivalent

f₁, f₂. F₃ etc. are multiplying factors for the alloys.

Accuracy of Predictions of Strength from Composition and Section

It is well known that no means of computation could accurately predict the tensile strength from the composition since many other variables exist which can greatly affect the properties. Angus, Dunn, and Marles¹² indicated a maximum variation of 6000 psi at any given value of carbon equivalent with scattered points lying outside the limits, usually on the low side.

In order to determine how accurate the prediction described in this paper might be, predictions were made for a large number of heats reported in the literature and compared with the experimental results. Data so studied were from Burgess and Bishop,³¹ Barlow and Lorig,¹⁴ Crosby,³² Young, Crosby and Herzig,³³ Bolton,³⁴ and a large number of commercial heats.

The experimental values were divided by the predicted values. If the predicted strength was exactly equal to the actual strength, the quotient is 1 or 100 per cent. The values were subdivided into divisions, plus and minus, in 5 per cent increments. A statistical study was made and is reported in Fig. 19, 20 and 21.

Figure 19 shows the distribution of values obtained from close to 300 irons, plain and alloyed and of various section sizes. From this curve the prediction seems about 1 per cent low on the average. Of the total number of irons studied the predicted strength was less than \pm 5 per cent different from the experimental strength in over 37 per cent of the cases; less

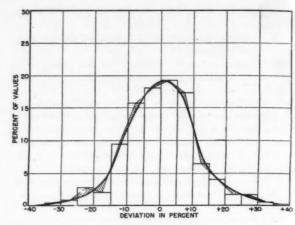


Fig. 19—Accuracy of Predictions of Tensile Strength of 300 Heats of Plain and Alloyed Irons Factor Used: Experimental Strength

Predicted Strength

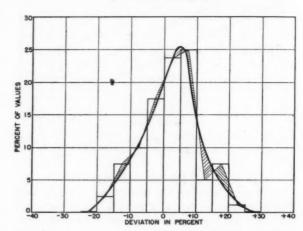


Fig. 20-Accuracy of Predictions of Tensile Strength of 80 Heats of Alloyed Irons.

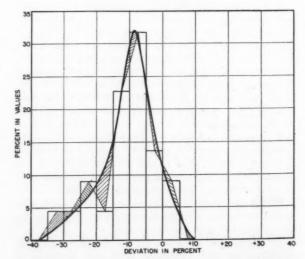


Fig. 21—Accuracy of Predictions of Tensile Strength of 22 Heats of Uninoculated Induction Melted Irons.

than \pm 10 per cent different from the experimental strength in over 70 per cent of the cases; and less than \pm 15 per cent different from the experimentally determined strength in almost 87 per cent of the cases.

Figure 20 shows a special study of 80 heats of alloyed cent which means that the predicted strength averaged

5 per cent low.

Figure 21 presents 22 heats of uninoculated irons melted in an induction furnace by Burgess and Bishop.³¹ Here the maximum frequency comes at about –8 per cent with more values below than above this figure. In other words, the predicted value is high or the experimental values are low compared to prediction. This confirms the findings of many researchers using the induction furnace. Inoculation in every case involving the 22 heats described here brought the actual tensile strengths very close to, either slightly lower or higher than the predicted values.

The heats described in Fig. 20 and 21 are included

in the heats shown in Fig. 19.

Carbon equivalent = 3.532

Unalloyed Base Strength = 25,460 psi

Further confirmation that the method of prediction of strength from the chemical composition and section size of sand castings is at least approximately accurate is shown in the four following examples.

a. Piston ring iron (MacCready**)

| | | | 1 | Strength |
|-----|--------------|----------------------|----------------------------|-----------------|
| | Analysis | Section, | Strength, | (Computed) |
| | Per Cent | in. | psi | psi |
| C | = 3.8 | 0.125 | 40-45,000 | 42,800 |
| Si | = 2.7 | 0.250 | about 35,000 | 34,650 |
| P | = 0.4 | 1.2 | 18-20,000 | 18,980 |
| | | b. Large Casting | (MacKenzie ⁸⁵) | |
| | | Analysis, I | Per cent | |
| | | C = 8 | 3.05 | |
| | | Si = 1 | .22 | |
| | | P = 0 | 0.30 | |
| | | Mn = 1 | .16 | |
| | | Mo = 0 | .90 | |
| | | V = 0 | 0.06 | |
| | | Ni = 0 | 0.03 | |
| | | Cr = 0 | .03 | |
| Cas | ting 10-in. | diam and 36-in. lo | ng | |
| | 0 | ter = 38,600 psi | 0 | |
| | nputed stren | | | |
| | | $D_e = 8.78$ in. (no | areas considere | ed for gate and |
| 1 | | - , | or) | Sare and |

Alloy factor = (1.046) (1.405) (1.08) (1.0015) (1.012) = 1.551Computed strength = (1.551) (25,460) = 39,500

c. Large Hollow Cylindrical Casting (Hambley**)
Wall thickness 3 in., height 135 in., flange at ends 61½6 in. x 4 in.
Analysis, per cent
C. — 2.9 Tensile Strengths.

= 2.9Tensile Strengths, = 1.2660,000 psi min Si 2.0-in. diam C bar = 0.08bar 3.0-in. diam, 12.0 in. long about 52,000 psi P about 45,000 psi Mo = 0.653.0-in. wall section Ni = 1.75flange section about 45,000 psi = 0.50

Computed Strengths
Carbon equivalent = 3.34

Alloy factor = (1.292) (1.0875) (1.04) = 1.460Computed Unalloyed Base Section Do, in. Strength, psi Strength, psi C bar 2.00 43,200 63,000 3-in. bar 2.684 40.500 59,000 3-in. wall 6.00 32,100 46,900 5.96 32.200 47,000 Flange

d. Agreement with the results of Angus, Dunn, and Marles.¹² Lines obtained by plotting tensile strength

computed by the equation

T = 10,000 (11.68 — 2 C.E. — 2.3 log D) have been superimposed on the graphs presented by Angus, Dunn and Marles. It was found that these lines are all well within the limits of their findings. The line for 0.6-in. diam is on the low side. It is pointed out that the carbon equivalent has been computed differently by the present writers than by Angus, Dunn, and Marles. Most of the values in the graph have a high phosphorus content, around 1 per cent. If a correction for this difference is made, the new line will be shifted about 0.08 units of C.E. to the left and better agreement is obvious.

From the frequency curves and the four examples cited it would seem that predictions of strength of gray irons made on the basis of composition and sections cast in sand can be computed with a fair degree of accuracy. The results so obtained seem to agree at least within \pm 10 or \pm 15 per cent of the experimental results. All of the data used in obtaining the above correlation have been taken from irons made under commercial foundry conditions with the exceptions of some laboratory irons reported by Crosby and by Burgess and Bishop. Inoculation, superheat, and variations in raw materials and foundry practices are probably responsible for the fact that closer predictions cannot be made.

PART II

CORRELATION OF PROPERTIES OF GRAY IRONS

By

R. G. McElwee and Richard Schneidewind

PROPERTIES OF GRAY IRONS most frequently considered are tensile strength, Brinell hardness, transverse strength, compressive strength, modulus of elasticity, and impact. It is not always feasible or possible to test for all these properties but one property is usually determined: either hardness or strength, tensile or transverse.

Due to the wide variation in properties resulting

from changes in composition, cooling rate, molding and foundry practice it would be dangerous to propose that any rigorous expression could be found which would indicate the magnitude of one property if some other property were known, for example, to compute the tensile strength if only Brinell hardness had been determined.

Tensile Strength and Brinell Hardness

MacKenzie³⁷ made a statistical study of tensile-Brinell relationships which may be expressed as $T=1.82~\mathrm{B}^{1.85}$

where T = tensile strength in psi, B is Brinell hardness number. This equation expresses the statistical average relationship although actual experimental values may vary over wide limits.

For example, if T=50,000 psi, B has been found to vary from a low of 207 to a high of 350 with the statistical average being 250.

Unusually for a given value of tensile strength, a low hardness is desirable from the standpoint of machinability.

A quality index could, therefore, be set up from the tensile hardness values of a given iron. If these values are inserted in MacKenzie's general expression, $T = aB^{1.85}$, then the constant, a, can be evaluated. If the value, a, is divided by the statistical average a, which is 1.82, this quotient could be a quality index.

For example: T = 40,000 psi, B = 200, the value of a can be computed as 2.22. The quality index would be

$$2.22 = 1.22$$
 or 122 per cent 1.82

If B = 250, a = 1.47 and the quality index is 1.47 = 0.81 or 81 per cent Fig. 22 has been drawn 1.82

showing MacKenzie's statistical average relationship

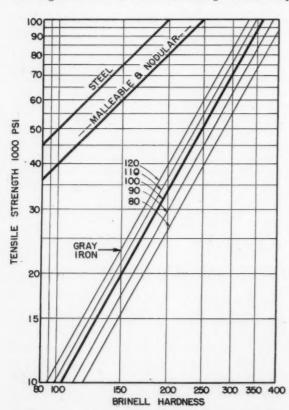


Fig. 22-Tensile Strength-Brinell Hardness Relationship (MacKenzie).

in the heavy line. The finer parallel lines show the quality index ranges from 80 to 120 per cent.

For the purpose of showing the strength to hardness relationship of gray irons in comparison with other materials two additional lines have been drawn. The one marked steel has a fixed 500 to 1 ratio between T and B and is a good approximation of the behavior of steels. The second line has been drawn at a 400 to 1 ratio. Malleable irons and pearlitic malleable irons have been shown⁴⁸ to have a constant tensile to Brineil ratio of around 420, just above the line drawn. Completely nodularized gray irons also plot approximately on the 420 line. Nodular irons, incompletely nodularized, fall between this line and the line for gray irons. It is significant that incompletely nodularized irons of say, 60,000 psi, may have a Brinell hardness of 200.

Tensile Strength and Transverse Strength

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It has frequently been stated that there is no good relationship between the tensile strength and the modulus of rupture in bending. MacKenzie³⁷ has summarized his findings in the form of ratios of modulus of rupture to tensile strength. He shows this ratio to vary with the tensile strength. He also showed from statistical analysis of a large number of bars the following relationship

$$T = 0.83 M R - 19,997$$

where T = tensile strength and MR = modulus of rupture, both in psi. This equation can be simplified to read as follows:

$$MR = 1.2 (T + 20,000)$$

Angus, Dunn, and Marles¹² also found a linear relationship between these two properties with values deviating \pm 4500 psi in tension for any given value of MR. Their line may be expressed as

$$MR = 1.4 (T + 10,000)$$

They state further that the results of Schaum³⁸ fit this line even better than do the results which they themselves obtained.

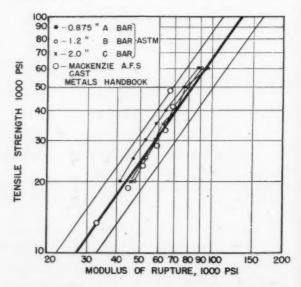


Fig. 23—Tensile Strength-Transverse Strength (Modubus of Rupture) Relationship. ASTM Specifications Plotted.

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TABLE 3—TENSILE STRENGTH AND MODULUS OF RUPTURE (MACKENZIE)

| Tensile Strength, | | | Mod | upture psi | |
|----------------------|-------|--------|--------|------------|----------|
| psi | Tests | Max | Min | Aver. | Computed |
| 13,400 | 2 | 33,000 | 32,400 | 32,800 | 32,750 |
| 18,600 | 3 | 51,000 | 39,000 | 45,000 | 41,600 |
| 23,000 | 5 | 60,800 | 46,200 | 51,300 | 48,500 |
| 28,200 | 2 | 60,500 | 57,400 | 59,000 | 56,000 |
| 32,600 | 6 | 69,500 | 59,400 | 63,900 | 60,500 |
| 41,200 | 3 | 72,500 | 66,000 | 68,900 | 70,900 |
| 47,200 | 1 | - | - | 66,500 | 79,400 |

Figure 23 was drawn with $\log T$ plotted against $\log MR$ where T= tensile strength in psi and MR= modulus of rupture in psi. First the ASTM specifications (A-48-41) were plotted on this graph for 0.875-in., 1.2-in., and 2.0-in. bars. It was assumed that, for example, Class 30 meant that the A, B, and C bars each possessed a tensile strength of 30,000 psi. Note that all three lines so drawn lie close together and are parallel. MacKenzie's³⁵ values were next plotted (Table 3). Finally, all the values published in papers by Crosby,³² Young, Crosby and Herzig,³³ Burgess and Bishop,³¹ the gray iron values of Morrogh,³⁰ and the data presented by Archer, Briggs, and Loeb,⁴⁰ and many commercial results were all plotted on this graph.

The actual points have been omitted from the graph for publication but the lines showing the maximum spread have been reproduced. The best average line by inspection) was drawn. The equation of this average line is

$$\log T = 1.43 \log M R - 2.33 \quad \text{or} \\
T = \left(\frac{M R}{42.6}\right)^{1.43}$$

For further confirmation the following computations were made:

1. For various values of carbon equivalent and for diameters of 0.6, 0.875, 1.2, and 1.6 in., the tensile strength was computed in the manner described earlier

2. From these values of tensile strength the equivalent modulus of rupture was computed.

3. These values for modulus of rupture (transverse strength) were plotted against the carbon equivalent for each of the diameters mentioned in (1).

4. The limit lines obtained by Angus, Dunn, and Marles¹² were drawn in.

It may be seen from Fig. 24 that the computed moduli of rupture fall in the middle of the Angus, Dunn and Marles bands.

If the tensile-transverse data of Schneidewind and White⁴¹ for pearlitic malleable irons, of Schneidewind and Hoenicke⁶ for annealed permanent mold gray irons and of the fragmentary data on nodular cast irons are plotted on this graph, the points cluster around the limit line to the right of the average line for gray irons in Fig. 23.

It would seem that if the relationship $T = \left(\frac{MR}{426}\right)^{1.43}$ is used with discretion, it may prove

valuable in predicting at least roughly, the modulus of rupture and the transverse breaking load from ten-

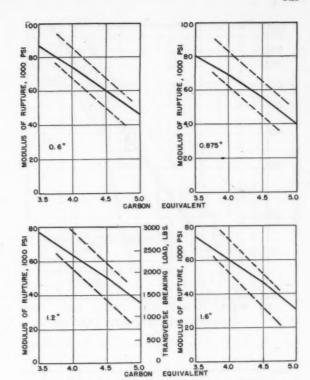


Fig. 24—Comparison of Predicted Tensile and Transverse Strengths Computed from the Carbon Equivalent with the Results of Angus, Dunn and Marles.

sile values. For all practical purposes the equations of MacKenzie and of Angus, Dunn, and Marles are equally usable since the values of modulus of rupture corresponding to tensile strengths between 20,000 and 60,000 psi are almost identical. The logarithmic equation above predicts values closer to those of Angus, Dunn, and Marles than to those of MacKenzie.

Tensile Strength and Compressive Strength

The literature on compression tests of gray irons is not extensive. Perhaps the best compilation of results may be found in the Cast Metals Handbook. Table 4 presents data worked out by Moore, 42 Piwowarsky, 48 Draffin and Collins, 44 Campbell, 45 Kommers 46 and by an unnamed worker reporting on nickel cast iron. In most cases, the results had been given in the form of a ratio between compressive strength and the equivalent tensile strength. In this table the actual compressive strengths have been recomputed.

In Fig. 25, the results of Moore and of Kommers have been plotted on a log-log scale. Limit lines to indicate maximum expected deviations have been added. An average line through these data has the equation

$$\log T = 1.43 \log C - 2.71 \qquad \text{or} \\
T = \left(\frac{C}{78.5}\right)^{1.43}$$

where T = tensile strength in psi and C = compressive strength in psi.

Naturally, the other data were used in the original plots to arrive at the average relationship. Comparison

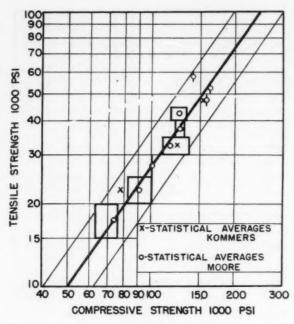


Fig. 25-Tensile-Compressive Strength Relationship.

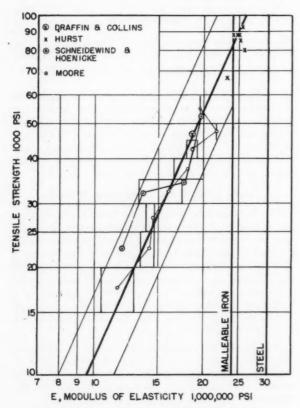


Fig. 26-Tensile Strength-Tensile Modulus of Elasticity Relationship.

of Table 4 and the graph will show that all the data fall within the limit lines. Draffin and Collins got wide differences in compressive strength on the same iron by altering the length of the test specimen. The

TABLE 4-RELATION BETWEEN TENSILE AND

| No | 0. 0 | f Tens | sile Stre | ngth | Comp | ressive S | trength, | Con: | |
|-----------|------|---|-----------|----------|----------|-----------|-----------|----------|--|
| Source To | est | s Max. | Min. | Avg. | Max. | Min. | St. Avg. | puter | |
| Impact | 4 | 20,000 | 15,000 | 17,500 | 76,000 | 62,900 | 71,400 | 77,500 | |
| Report | 4 | 25,000 | 20,100 | 22,550 | 101,200 | 82,600 | 90,500 | 88,00 | |
| | 2 | 30,000 | 25,100 | 27,550 | 101,800 | 101,000 | 101,400 | 101,00 | |
| | 7 | 35,000 | 30,100 | 32,550 | 137,000 | 109,800 | 117,300 | 113,00 | |
| | | | | | | 121,200 | | | |
| | 2 | 45,000 | 40,100 | 42,550 | 133,300 | 121,300 | 127,300 | 137,000 | |
| | 1 | 50,000 | 45,100 | 47,550 | | | 157,600 | 148,000 | |
| | 1 | 55,000 | 50,100 | 52,550 | | | 162,000 | 159,000 | |
| | 1 | 60,000 | 55,100 | 57,550 | | | 141,000 | 169,000 | |
| Kommers | 1 | 25,000 | 20,100 | 22,550 | | | 77,000 | 88,000 | |
| | 4 | 35,000 | 30,100 | 32,550 | 131,200 | 115,200 | 124,800 | 113,000 | |
| | 3 | 40,000 | 35,100 | 37,550 | 139,100 | 116,600 | 129,000 | 125,000 | |
| | 1 | 50,000 | 45,100 | 47,550 | | | 138,500 | 148,000 | |
| | 1 | 55,000 | 50,100 | 52,550 | | | 146,000 | 159,000 | |
| | _ | | Tensi | le Stren | gth. Co | mpressi | ve Strens | rth. psi | |
| Source | | | psi | | Reported | | | puted | |
| Piwowarsh | ci | K. C. | 30,000 | | 128,000 | | 108,000 | | |
| | | | 9 | 31,000 | 1 | 33,800 | 11 | 0,000 | |
| | | | 5 | 34,100 | 1 | 28,500 | 11 | 8,000 | |
| | | | 41,500 | | 141,000 | | 135,000 | | |
| Alloyed C | ast | Irons | 23,870 | | 96,090 | | 92,000 | | |
| | | | - 4 | 41,020 | | 125,910 | | 132,000 | |
| | | | 7 | 78,200 | 2 | 15,400 | 21 | 2,000 | |
| | | | 7 | 72,700 | 2 | 03,000 | 20 | 00,000 | |
| | | | 8 | 30,200 | 1 | 87,500 | 21 | 4,000 | |
| | | | 78,200 | | 2 | 15,000 | 212,000 | | |
| | | 7 | 5,000 | 2 | 13,000 | 20 | 2,000 | | |
| Campbell | | 5 | 0,800 | 1. | 52,000 | 15 | 4,000 | | |
| | | | 4 | 13,800 | 1 | 76,000 | 14 | 0,000 | |
| | | | 5 | 60,900 | 1 | 90,000 | 15 | 5,000 | |
| | | | 3 | 8,100 | 1: | 29,000 | 12 | 8,000 | |
| | | | 3 | 14,800 | 1 | 49,000 | 11 | 8,000 | |
| | | | 9 | | | | | | |

lengths were 3 and 6 times the diameter respectively. These data, too, fall within the limits.

32,200

34,900

28,800

26,000

29,200

26,100

23,300

23,700

21.600

46,800

Draffin and Collins

*1/d = 3

130,000

125,000

117,000

96,200

99,000

98,000 82,800

98,000

80,000

152,500*

116,400**

** 1/d = 6

113,000

118.300

103,000

94,000

104,000

94,300

90,000

92 000

85,000

146,000

146,000

Compressive strengths were computed by the above equation for all the irons reported and these values are included in the table. It would seem that if the limits of variation are clearly understood the method of conversion here proposed will more conveniently give the design engineer an idea of the compression properties than the more cumbersome ratios which change with tensile strength.

Tensile Strength and Modulus of Elasticity in Tension

The design engineer is more concerned with tension modulus than the foundryman. Since gray iron does not follow Hooke's Law exactly, the results of experimental work are liable not to check too well unless by convention it is agreed that no values above about 25 per cent of the breaking strength are used.

Table 5 presents in tabular form the results of

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TABLE 5-TENSILE STRENGTH AND TENSILE MODULUS

| Tens | ile Strength, | Tensile Mo | Tensile Modulus, psi X10 | | |
|---------------------|---------------|------------|--------------------------|--|--|
| Source | psi | Reported | Computed | | |
| Draffin and Collins | 46,800 | 18.5 | 18.60 | | |
| Schneidewind and | 52,267 | 19.53 | 19.70 | | |
| Hoenicke | 34,142 | 17.57 | 16.40 | | |
| Permanent Mold Iron | 32,050 | 13.60 | 16.20 | | |
| | 22,533 | 11.98 | 13.95 | | |
| Hurst | 93,000 | 25.5 | 25.15 | | |
| | 88,100 | 24.9 | 24.90 | | |
| | 85,400 | 25.1 | 24.80 | | |
| | 90,000 | 25.8 | 25.10 | | |
| | 88,800 | 24.9 | 24.92 | | |
| | 87,900 | 24.7 | 24.88 | | |
| | 67,200 | 24.2 | 22.40 | | |
| | 87,900 | 24.0 | 24.88 | | |

| | No. of | Tensil | e Streng | th, psi | Modulus of Elasticity, psi x l Com | | | |
|--------|--------|--------|----------|---------|---------------------------------------|----------|------|-------|
| Source | Tests | Max. | Min. | Avg. | Max. | Min. St. | Avg. | puted |
| Moore | 4 | 20,000 | 15,100 | 17,550 | 12.8 | 10.4 | 11.6 | 12.4 |
| | 4 | 25,000 | 20,100 | 25,550 | 14.6 | 13.3 | 14.2 | 13.8 |
| | 2 | 30,000 | 25,100 | 27,550 | 14.6 | 14.3 | 14.5 | 14.8 |
| | 7 | 35,000 | 30,100 | 32,550 | 17.4 | 13.2 | 16.0 | 15.9 |
| | 3 | 40,000 | 35,100 | 37,550 | 19.9 | 16.5 | 18.1 | 17.4 |
| | 2 | 45,000 | 40,100 | 42,550 | 19.1 | 17.9 | 18.5 | 18.2 |
| | 1 | 50,000 | 45,100 | 47,550 | | | 22.6 | 19.0 |
| | 2 | 60,000 | 50,100 | 55,050 | | | 19.5 | 20.0 |

Moore,⁴² Draffin and Collins,⁴⁴ Hurst⁴⁷ on normal gray irons and of Schneidewind and Hoenicke⁶ on annealed permanent mold gray irons. These data have been plotted on a log-log scale in Fig. 26. An average line has been fitted in by inspection. Two limit lines show the maximum variation shown by these data.

The equation of the average line is

$$\log T = 2.32 \log E - 12.2 \quad \text{or} \\
T = \left(\frac{E}{181,400}\right)^{2.32}$$

where T = tensile strength in psi and E = tensile modulus of elasticity in psi at 25 per cent of the breaking strength. The deviation of results from the predicted average is high but if the equation is used simply as a guide it may be of considerable use to the designer.

Moduli of elasticity have been computed for the data in Table 5 and have been included in the table. The graph also contains a line for steel and one for malleable irons annealed or pearlitic.

Areas of Microstructure

It is obvious that the Maurer diagram does not accurately portray the behavior of cast irons. The principal interest of the foundryman making gray irons is the region of pearlitic iron free from ferrite and from cementite.

Sipp,⁴⁹ and Angus, Dunn, and Marles,¹² each correlated this region against the carbon equivalent and the section size in sand castings. On a plot with section as the ordinate and carbon equivalent as the abscissa two lines were drawn. One line marked the limit of composition and section where pearlitic gray iron was changing to pearlitic iron plus carbides. The other indicated where the pearlitic changed to pearlitic iron plus ferrite.

The structure areas are outlined in Fig. 27 by

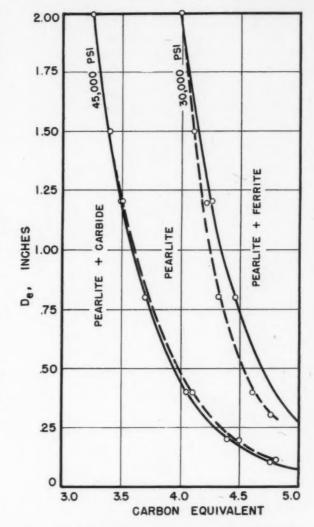


Fig. 27—Microstructure of Gray Irons as Affected by the Carbon Equivalent and Section Size.

dotted lines as given by Angus, Dunn, and Marles. The superimposed solid lines are the parameters for 30,000 and 45,000 psi in tensile strength as computed in this paper.

It would seem that for ordinary unalloyed, uninoculated irons, mottling sets in when a tensile strength of about 45,000 psi is reached. Similarly, ferrite is encountered in these irons which have a strength near 30,000 psi. These limits correspond to 235 and 188 Brinell by computation, respectively.

Suggested Rapid Approximation of Mechanical Properties

In order to make rapid approximations of mechanical properties the following procedures are suggested. It must be appreciated that these do not purport to be rigidly accurate but if used with judgment, may prove convenient.

For a given section size and composition (unalloyed) the tensile strength may be approximated by either of the following methods.

T = 10,000 (11.68 - 2 C.E. - 2.3 log D)

or if Fig. 14 is consulted, find constant b for any desired section. Then

$$T = 10,000 (b - 2 C.E.)$$

The carbon equivalent, C.E. is defined as %C + % Si + % P.

Another method employs the log-log relationship which is best demonstrated in Fig. 28. A line is drawn

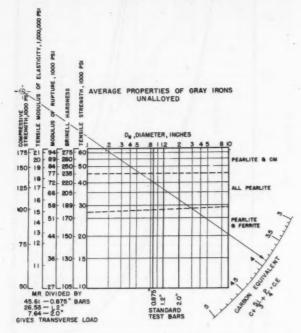


Fig. 28—Tensile Strength of Unalloyed Gray Irons as Affected by the Carbon Equivalent and Section Size.

from the "point of rotation" to the desired carbon equivalent. By finding the point of intersection of this line with the desired section size, the tensile strength can be read directly on ordinate at left.

Once the tensile strength has been determined, it can be multiplied by suitable factors for alloying elements.

Figure 29 has been prepared in order to convert

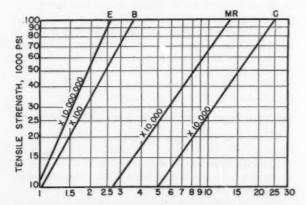


Fig. 29—General Approximate Correlations between Tensile Strength and Hardness, Modulus of Rupture, Modulus of Elasticity, and Compressive Strength.

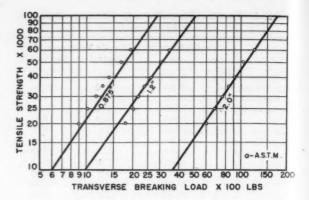


Fig. 30-Transverse Breaking Loads for A, B, and C Arbitration Bars for Iron of Different Tensile Strengths.

tensile strength to probable values of hardness, modulus of rupture, modulus of elasticity and compressive strength by direct reading. Figure 30 permits the computation of approximate transverse breaking loads from tensile strength. Three methods of computation are compared in Table 6.

TABLE 6-COMPUTED TRANSVERSE BREAKING LOADS, LB

| Tensile Strength, psi | ASTM | ADM | SM | Mac |
|--------------------------|---------|--------------|--------|--------|
| - For | | | | |
| 00.000 | | 875-in. bars | 071 | 1050 |
| 20,000 | 900 | 922 | 975 | 1052 |
| 25,000 | 1025 | 1075 | 1119 | 1185 |
| 30,000 | 1150 | 1230 | 1270 | 1316 |
| 35,000 | 1275 | 1372 | 1405 | 1448 |
| 40,000 | 1400 | 1535 | 1555 | 1580 |
| 50,000 | 1675 | 1840 | 1745 | 1840 |
| 60,000 | 1925 | 2150 | 2070 | 2105 |
| 80,000 | | 2760 | 2455 | 2630 |
| | | 1.2-in. bars | | |
| 20,000 | 1800 | 1585 | 1630 | 1810 |
| 25,000 | 2000 | 1848 | 1900 | 2040 |
| 30,000 | 2200 | 2110 | 2150 | 2265 |
| 35,000 | 2400 | 2370 | 2395 | 2495 |
| 40,000 | 2600 | 2640 | 2670 | 2720 |
| 50,000 | 3000 | 3160 | 3000 | 3160 |
| 60,000 | 3400 | 3695 | 3520 | 3700 |
| 80,000 | | 4750 | 4310 | 4520 |
| | | 2.0 in. bars | | |
| 20,000 | 6000 | 3500 | 5700 | 6150 |
| 25,000 | 6800 | 6420 | 6620 | 7075 |
| 30,000 | 7600 | 7330 | 7440 | 7850 |
| 35,000 | 8300 | 8250 | 8350 | 8650 |
| 40,000 | 9100 | 9160 | 9270 | 9420 |
| 50,000 | 10300 | 11000 | 10400 | 11000 |
| 60,001 | 12500 | 12840 | 12200 | 12590 |
| 80,000 | | 16480 | 14680 | 15700 |
| | Modulus | of Rupture | e, psi | |
| 20,000 | | 42,000 | 43400 | 48000 |
| 25,000 | | 49000 | 50600 | 54000 |
| 30,000 | | 56000 | 56900 | 60000 |
| 35,000 | | 63000 | 63900 | 66000 |
| 40,000 | | 70000 | 70900 | 72000 |
| 50,000 | | 84000 | 79500 | 84000 |
| 60,000 | | 98000 | 93400 | 96000 |
| 80,000 | | 12600 | 114100 | 120000 |

ASTM American Society of Testing Materials

ADM Angus, Dunn and Marles

SM Schneidewind and McElwee

Mac MacKenzie

As a final example of the method of procedure, let it be assumed that a foundry is required to meet a brake drum specification such as is quite common for heavy vehicles.

Total carbon: 3.40 per cent minimum

Silicon: at the discretion of the foundry

Alloys: optional

Strength: 40,000 psi in tension in 1.2-in.

arbitration

By referring to Fig. 15, it is found that in a 1.2-in. diam section a carbon equivalent of 3.75 per cent will yield an iron of 40,000 psi in tension. The silicon content can be computed as follows:

 $(3.75 - 3.40) \times 3 - 1.05\%$, the indicated silicon)

However, the foundry in question finds it advantageous to run a 1.70 per cent silicon because of other work to be poured at the same time. The carbon equivalent of this run (3.4 per cent C minimum) is 3.40 + 1.70 + 0.16 = 4.01 per cent assuming 0.16 per 3

cent phosphorus.

C

u

ds

n

Again from Fig. 15, it is seen that a carbon equivalent of 4.01 per cent in a 1.2-in. section will produce an expected 35,000 psi in tension. By dividing the desired final strength of 40,000 psi by 35,000 psi, the amount of improvement necessary can be computed

$$\frac{40,000}{35,000} = 1.142$$

This means that the base iron must be strengthened 14.2 per cent by alloying. About 0.32 per cent Mo or 0.36 per cent Cr will accomplish this as can be computed from Fig. 18. If any danger from chilled edges or eutectiform graphite is expected, a small quantity of ladle inoculant can also be added. With these data a working specification can be established.

The writers appreciate that for as complex and sensitive a material as cast iron, no absolute relationships can be written between chemical composition, section size, and mechanical properties even though the work is limited only to sand castings. It is hoped that the computed results presented here will prove to be close to the true average values obtained in the foundry. If they do, then the confusion and variations will, in effect, be cut in half. Undoubtedly, many of the differences in results encountered in cast irons are due not only to heterogeneity of the material but also to inaccuracies in reported chemical compositions, dimensions of test bars, and a lack of meticulous care in determining mechanical properties. With better data, probably closer predictions of properties can be made especially if the industry can attain closer control of such factors as sand, superheat and pouring temperatures, and inoculation schedules.

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DISCUSSION

Chairman: F. J. Walls, International Nickel Co., Detroit.

Co-Chairman: T. D. Parker, Climax Molybdenum Corp.,
New York.

H. A. SCHWARTZ AND W. K. BOCK (Written Discussion)¹ We, in the malleable iron industry, know little about the properties of gray iron but we are vitally interested in the mottling question. Since the present authors have given a new quantitative approach to this problem, we should like to offer a somewhat extended discussion of this aspect of their work.

The Maurer diagram was based on so little factual information as to justify discarding it in any serious study of the

problem.

The present authors base their discussion on the observations of Uhlitzsch and Weichelt, without giving any of the confirming data. The discussion of their work by Piwowarsky is also in the form of conclusions without confirming data and we are left without any basis for determining whether their work was or was not careful. Weichelt's original Doctorial thesis is not available to use and the only detail in Piwowarsky's book is that the metal was crucible melted. The authors commented on this.

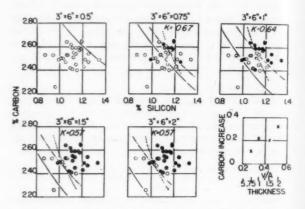
Laplanche's "A New Structural Diagram for Cast Iron," Metal Progress, vol. 52, no. 6, page 991 (1947) suggested the use of a

constant:-

$$K = \frac{4}{3} \operatorname{Si} \left(1 - \frac{5}{3C + \operatorname{Si}} \right)$$

as a measure of mottling tendency. He justified this value on theoretical grounds and appears to have substantiated it experimentally but gives no data confirming the latter fact. Finding so little factual evidence it seems justified to offer even a limited range of observations which, as it turns out, give reason for preferring the present authors' deductions to those of Laplanche.

We have at hand 30 series of five castings each, poured from 30 duplexed malleable heats (cupola-air furnace). All the castings are 6 in. long and 3 in. wide and the five castings in a set are respectively $\frac{1}{2}$ in., $\frac{3}{4}$ in., 1 in., $\frac{1}{2}$ in. and 2 in. thick. They are a portion of an investigation undertaken in a study of some of the other variables referred to by the authors and the present series represent only such variations as occur in the usual melting practice of the plant where they were made. We seek evidence whether in this circumscribed field, the limited number of specimens will permit us to decide whether the Schneidewind-McElwee deductions or those of Laplanche are a better guide to the boundary between clear and mottled iron.



In five of the six diagrams of the accompanying figure the carbon and silicon of the 30 heats are plotted. Each of the five refers to a specific sized casting, as indicated. Open circles show no visible mottle; dots represent composition in which any mottle at all, from the smallest visible amount up, could be seen.

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The solid lines show the authors' limits of mottling allowing for section size and form by these methods. All these lines are too low and require shifting as indicated in the original paper. We made this shift by changing the value of "b" in the authors' notation.

It is never possible to divide sharply between clear and mottled fractures. This the authors have anticipated since they rightly refer to the effect of other variables than composition.

We have also plotted the Si-C relation corresponding to that Laplanche constant which yields the closest agreement with the chosen line of the Schneidewind-McElwee form. It seems to us quite clear that the relation of carbon and silicon developed in the present paper furnishes a decidedly better measure of mottling tendency than the Laplanche deductions.

We now come to the shift of the lines and its relation to section size. The authors here use Chvorinov's assumption that freezing time, and hence cooling rate, can be predicted by the ratio of volume to area. There is no perfect agreement on this point. (See "Comparative Solidification Studies" by V. Paschkis, Transactions, A.F.S., vol. 57).

The five sizes of blocks for a given heat were all poured into a single mold so that any variations in shift are due solely to the varying shape factors of the blocks. If the conversion to an equivalent diameter were perfectly reliable, the shifts should all be alike. If they differ systematically, the most likely conclusion is that the V/A criterion is insufficient.

In the sixth figure of the chart the amount of the shift (carbon increase) is plotted against the block thickness and its V/A value (gate corrected for). Unfortunately the results are not too conclusive, especially because we do not know how far the curve (for ½-in. sections) could be raised, since we never encountered mottles therein. There is a general trend for the shift to increase with increasing section thickness. That is, as the section size increased, increasingly higher carbons could be tolerated as compared with the authors' predictions.

Actually the shifted curves are nearly coincident for 3/4-in. and

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1-in. plates and for 11/2-in. and 2-in. plates presumably more accurate data would be required, to speak with any certainty as to the magnitude of the required correction to the authors' treatment. It does seem likely that the authors' conversion gives relatively high values for equivalent diameter on thick blocks as compared with thin. Since we can use relatively higher carbons we must be cooling relatively fast.

H. T. Angus (Written Discussion): I had the privilege of discussing some of the results in this paper with Prof. Schneidewind during a recent visit to America. This work correlates statistically the evidence which has been put forward by many previous workers including those put forward by my associates and myself last year. It would appear that a really solid basis now exists for the approximate estimation, based on composition, of the mechanical properties of gray iron castings. As the authors have pointed out, the relationships are an approximation only but can be regarded as very serviceable provided their limitations

I am rather inclined to doubt the advisability of changing the phosphorus factor from 3 to 4. All the evidence that has been quoted refers to work which was carried out prior to 1910 and in most cases practically no experimental values below a figure of 1 per cent of phosphorus are given. According to our

reading of the data in the original papers, $\frac{P}{4}$ fits the figures by

Fettweis, but $\frac{P}{2}$ and $\frac{P}{3}$ seem to fit the figures of Wüst, Goerens and Dobblestein, and also those of Stead. As the formula is an empirical one and convenience of use is of importance, it seems a little doubtful whether the evidence warrants changing the factor from 3 to 4, particularly as the effect in any but high phosphorus irons (e.g. above 1.5 per cent) is likely to be within the range of normal experimental error.

The chart (Fig. 28) relating the various mechanical properties to the carbon equivalent value provides a very convenient method of estimating the likely properties. The Brinell hardness, of course, is subject to the effect of phosphorus and it is customary to accept the figure of 40 Brinell increase for 1 per cent

phosphorus.

The tensile/Brinell ratio itself, however, is of doubtful value, particularly in the higher tensile region when comparing irons of different types. MacKenzie has shown that the scatter is far too wide to give useful information under these conditions. Dübi and Collaud, however, have shown that for any given iron, the relationship between tensile and Brinell is close and direct. Their work shows that the tensile/Brinell ratio of test bars poured at the same time as the casting can be used for the nondestructive testing of the casting itself by taking Brinell tests at significant points. Even here, of course, the results are only representative of the point at which the Brinell hardness is taken and not necessarily of the center of a heavy section where the hardness test is taken on the surface. Nevertheless, for castings of moderate thickness, for example, up to 2 in., the Dübi relationship is very valuable.

It is particularly interesting to observe that the log-log plot of tensile/section size shown in Fig. 16 is confirmed by curves based on our own and published results, both in relation to general form and slope, although our information has not gone beyond bar diameters of 6 in., and deviation from the straight-line relationship occurs in smaller diameter bars when chilling takes place. We believe that this paper shows for the first time that this relationship does exist. The use of the "equivalent test bar diameter" for relating the strengths of differing section thicknesses is worthy of close study by all foundrymen. Although it is an approximation and is subject to many qualifications, that should not deter anyone from making use of it as it gives results much closer to the actual findings than is possible by any other method short of the destruction of the casting itself.

With reference to the structure diagrams, it is important to remember that the modification of the Sipp diagram put forward by Angus, Dunn and Marles, endeavored to delineate only the point at which small quantities of carbide first become visible in the microstructure of the casting, although within the normal commercial range of composition, it will give a useful approximation to the production of unmachinable edges. We agree with MacKenzie that the carbon equivalent value does not give a good index of the point at which irons of widely different silicon content become completely white, or even largely mottled. The curves of Uhlitsch and Weichelt are more satisfactory in this respect but make no allowance for the effect of phosphorus. This, of course, is of less importance in American than in European foundry practice.

The correction factors for alloying elements which are given in this paper should prove useful to the engineer and the foundryman. The paper summarizes the accumulated data of many workers and provided the information is utilized with the caution advocated by the authors the results will prove of

great value to all users of cast iron.

MR. Schneidewind: The authors are grateful to Dr. Angus for his thoughtful and carefully considered discussion. We agree that for practical purposes a phosphorus factor of 3 or 4 is equally valid. It is pointed out, however, that the alloy factors for tensile strength are not claimed to be perfect; they are open to amendment as better experimental data are produced.

R. A. FLINN: ^a There is always a danger in going directly from alloy content to mechanical properties in a mathematical fashion without considering what is happening to structure. For example, in the evaluation of the effect of certain elements, let us take the case of 5 per cent for nickel. When you get a change in structure, take a composition which is on the boundary line going from pealitic to acicular you get a radical change in mathematical problems. So in using carbon equivalent, you cannot use a superfluous statistical relationship. The data in the paper covers a wide commercial range of cast irons, but as we get into newer varieties, such as the ductile irons or overacicular irons, you have to be pretty careful in the statistical evaluation.

Mr. Schneidewind: I appreciate Dr. Flinn's comments. We specifically limited this discussion to pearlitic or ferritic structure and on the alloy chart or alloy table we pointed out the maximum concentrations in any case. Actually, the acicular irons of Crosby plotted fairly close to predicted values. We pointed out that in alloyed iron we were consistently 5 per cent or more than that low in our predictions. But as long as we stay within the range of alloy that is limited here, and stay within those dotted lines on the chart, the predicted values are reasonably close.

JULES HENRY: 4 Cooling rate is a major factor in the correlation of physical properties and chemical composition. The paper does consider cooling rate, but calls it section size, and in that I am wholly in accord. However, I know that it is possible to take various size test bars and by adjusting the cooling rate so that it is the same, obtain the same physical properties. I feel that the cooling rate is one of the reasons for the differences in physical properties of the same iron as reported by various foundries. Some foundries will have a sand that cools much faster and others will have a sand that cools much more slowly.

I have done some fundamental work on the correlation of cooling rate and chemical analysis and attempted to alter the coo'ing rate by changing the shakeout time of the test bars. By pouring the same test bar in the same sand with the same chemical analysis, I have been able to change the physical properties by 30 per cent—not 5 or 10, but 30 per cent—just by altering the shakeout time!

The difference lies in the structure. A rapidly cooled iron will have a fined pearlitic structure than a slowly cooled iron of the same chemical analysis. The cooling rate I am thinking of is at the lower critical, around 1350 F. There have been other investigators in this field who have shown that the cooling rate through the lower critical is very important. I would like to carry that one step farther and investigate the effect of cooling rate of different sands and other mold materials.

As mentioned above, we can alter the physical properties of the same iron by 30 per cent. We can take an iron of a normal chemical composition with a 30,000 psi tensile, cast it in sand and hold it in the mold for the usual time until it is black. We

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can take that same iron, pour it in the same sand and shake it out in a much shorter time when the iron is above the lower critical, and we will get 36,000 to 38,000 psi tensile. I would like to have the authors' comments on this. Are we on the right track or is there something we have overlooked?

Mr. McElwee: Possibly the best answer that I could give to that would be to restate the purpose of the paper. The purpose of this paper is not in any sense to evaluate the skill or the knowledge of any one foundry in producing certain results. We are attempting to give in this paper good average values that can be handed to a design engineer in writing a specification which can be met by a large number of foundries. We have shown the spread which can be expected. If you, with your skill or with your knack of adjusting those conditions, can make your values appear above these lines, that is a matter for consideration between you and your customer. We still justify that the work that is presented here is an average which can be used for specification purposes with some degree of accuracy.

T. E. EAGAN: ⁸ The authors should be generously thanked for preparing this paper. It has long been needed by the gray

iron foundries.

Designers of castings have not been altogether satisfied with specifying the physical properties desired using separately cast test bars. This is true of castings made of any metal. What the designer wishes is the actual physical properties of the casting he is using. It is, of course, in most cases, not possible to furnish such information without destroying the casting. The separate test bar is therefore a compromise. Correlation of test bars with the physical properties of the castings they represent is therefore welcome.

The authors will agree that use of the information they have presented should be made with discretion. The equations will only apply to cast irons made under the same conditions as the examples used to determine them. For example: taking the tensile strength vs carbon equivalent using tests taken from large castings having sections from 1/2 in. to 3 in., we obtain an equation

$$\frac{\text{T.S.}}{10000} = 19.8 - 4.37 \text{ C.E.}$$

with a standard error of estimate of plus or minus 3350 psi. This does not agree with the equation given by the authors. I would be surprised if it did. We are dealing with an entirely different universe, in that these castings were from one foundry and we did not have too many tests to work with.

The difference between the two equations obtained is pointed out to show that instead of using the equation offered by the authors, it would be much safer to work one out for the individual foundry that proposes to use it. The authors have shown how it can be done and have given a general equation which could be entirely misleading if the methods of operation are different from those used by the authors. There is no question as to the validity of the authors equation. Any number of valid equations can be developed by using different figures which were obtained from entirely different sources.

The authors find good correlation between section size and tensile strength. Taking the results obtained from tests taken from the critical sections of 91 castings, with sections varying

from 1/2 in. to 3 in., we were not able to determine any influence of section size on tensile strength. We believe therefore, that the authors' equations can apply only to simple shapes of castings and not to the large complicated shapes. This, of course, is all related to the fact that even rather thin sections buried deep in sand and cores have extremely slow cooling rate.

The correlation of $\frac{V}{A}$ and cooling rate using simple shapes such as solid cylinders and plates is extremely good. However, when we come to plates with corrugations on them, or cylinders

with holes in them, the - to cooling rate relationship does not

always apply. We can become quickly confused should we assume that the cooling rate of a solid cylinder is the same as one with a core in it equal to half the diameter. I believe, therefore, that both the V and the A should be definitely defined.

There are so few places where simple shapes are used in castings and to assume that a casting can be divided into simple shapes and that each division will have the same cooling rate as

these simple shapes will generally not be true.

This paper is a valuable and a useful contribution to the gray iron foundry industry if its limitations are taken into consideration when using it. The best way a foundry can use it is to determine the same information from their own production and use factors to change the results given to fit their own conditions. A very few tests would be necessary to do this.

MR. SCHNEIDEWIND: We recognize that the cooling rate of the metal is the critical factor. The V/A is a rough approximation of cooling rate if the conduction rate from the metal to the mold material is constant. In steel two factors are used, called H and D, with D being diameter and H being the cooling rate. We have assumed that H is constant. We have no data as yet as to the comparative cooling rate of various sands or cores and sands that have been heated at the higher temperatures. That data has to be obtained by experimental work. To secure data for the charts in the paper, we used values for irons cast in sand. We covered castings as small in size as the automotive piston ring, 1/2 in. square, and there are many data from there up to about 3 in. in diameter. We have not been able to get a great deal of data on castings larger than 3 or perhaps 4 in. except for Dr. MacKenzie's 10-in. diam cylinder that we made back in 1929 or 1931. Perhaps our position may be a little weak in extrapolating to 10 in. in diameter.

C. O. Burgess: I disagree with the authors on one point in that this particular paper can be used very conveniently by foundries as a measure of the effectiveness of their own practice and whether their practice needs alteration. It is difficult to evaluate an iron with the scattered physical data that has been available up to this time. I think this paper is going to be a more useful tool than most foundrymen realize. In other words, the producer can quickly determine whether he is melting an iron that comes up to the average properties to be expected for a given carbon equivalent, cooling rate or section size.

J. T. MacKenzie: ⁷ The Brinell hardness, tensile strength ratio is a good indication of the toughness of the iron. By dividing the tensile strength by the Brinell hardness number you get a fairly

good index to the impact resistance of the iron.

⁵ Chief Metallurgist, The Cooper-Bessemer Corp., Grove City, Pa.

⁶ Technical Director, Gray Iron Founders' Society, Cleveland. ⁷ Technical Director, American Cast Iron Pipe Co., Birmingham, Ala.

CHARACTERISTICS OF SOME ALUMINUM-ZINC-MAGNESIUM-COPPER CASTING ALLOYS

By

W. E. Sicha* and H. Y. Hunsicker*

ABSTRACT

Investigations of aluminum-zinc-magnesium-copper alloys have led to the selection of two compositions, one for sand casting and another for permanent mold casting. The sand casting alloy, designated A612, has the nominal composition, 6.5 per cent Zn, 0.7 per cent Mg, 0.5 per cent Cu and 0.15 per cent Ti. The alloy designated C612, containing 6.5 per cent Zn, 1.0 per cent Fe, 0.5 per cent Cu, 0.35 per cent Mg and 0.10 per cent Ti has sufficient resistance to hot cracking to permit production in permanent molds of castings that are not extremely intricate in design.

Good combinations of tensile properties for nonheat-treated aluminum alloys are produced in these alloys by natural aging. A suitable artificial aging treatment may be applied to alloy A612 for more rapid attainment of specified tensile properties. Standard accelerated tests have indicated that these alloys are resistant to corrosion and to stress-corrosion cracking.

Both alloys exhibit excellent brazing characteristics and are used for castings in brazed assemblies. The mechanical properties and resistance to corrosion are not impaired by conventional brazing treatments at temperatures up to 1120 F, and are satisfactory after elevated temperature treatments which may be employed for aging wrought components of brazed products.

AN EXCELLENT HISTORICAL REVIEW of aluminum-base casting alloys with zinc or zinc and magnesium as major alloying additions was provided by L. W. Eastwood and L. W. Kempf.¹ Their investigations resulted in selection of a nonheat-treated aluminum alloy of this type, containing about 6.5 per cent Zn, 0.3 per cent Mg, 0.4 per cent Cu, 0.25 per cent Cr and 0.15 per cent Ti, which afforded an attractive combination of tensile properties.

Two characteristics of this alloy were considered to be susceptible to improvement. Relatively rapid initial aging at room temperature caused difficulty in establishing minimum tensile properties for specification purposes and most artificial aging treatments substantially impaired the tensile properties. A second disadvantage was the marked tendency for hot cracking to occur in permanent mold castings of this alloy.

Extension of the earlier work was directed toward overcoming these two disadvantages. This paper presents results of the more recent investigation which culminated in development of two alloys of this type that have been designated A612 and C612 alloys. The former is a sand casting alloy and the latter is intended for permanent mold casting production.

Experimental Procedure

Conventional foundry practices were employed in preparing experimental melts and test castings. Tensile properties reported are those of standard test bars that were cast-to-size, the one exception being specimens for corrosion tests which were machined in the test section.

Sand Casting Alloy

The sand casting alloy investigation was conducted with the object of attaining more rapidly the good tensile properties provided by several weeks of natural aging in the aluminum-zinc-magnesium alloy X612 developed by Eastwood and Kempf. An associated objective was development of a modification of this composition that would be less subject to reduction in tensile properties upon application of elevated temperature aging treatments that may be required for the wrought aluminum members of brazed assemblies.

Efforts to alter the aging characteristics of alloy X612 by means of small additions of tin or beryllium were unsuccessful. Similarly unsatisfactory results were encountered in tests on a combined quenching-aging treatment in which the test bar castings were shaken out of the molds as soon as possible, quenched in boiling water and held in the boiling water for various periods of time.

Effect of Zinc and Magnesium Contents—Some of the earlier results had indicated that increasing the magnesium content might impart the desired aging characteristics. Therefore, test bars of alloys containing 5.5 to 7 per cent zinc and 0.45 to 0.60 per cent magnesium, as well as 0.5 per cent copper, 0.15 per cent titanium, 0.15 per cent iron and 0.10 per cent silicon, were subjected to treatments at the commercial aging temperatures of 310 or 340 F. It was evident from these results and from previous observations that some reduction in typical tensile strength from the 36,000 psi value for alloy X612 would have to be accepted if aging at an elevated temperature were em-

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Assistant Chief, Cleveland Research Division, Aluminum Research Laboratories, Aluminum Company of America.

ployed. Actually the test results showed that only the alloy with the highest contents of zinc (7.0 per cent) and magnesium (0.60 per cent) exceeded the arbitrarily selected minimum tensile strength of 32,000 psi after aging for 2 hr at 310 or 340 F. An 8-hr treatment at 310 F provided the desired tensile strength in the four alloys containing 7.0 or 6.5 per cent zinc with 0.60 or 0.55 per cent magnesium. Aging 8 hr at 340 F expanded the range of compositions meeting the tensile strength requirements to include those alloys containing 7.0 per cent zinc and 0.60, 0.55, 0.50 or 0.45 per cent magnesium or 6.5 per cent zinc and 0.60 or 0.55 per cent magnesium. However, these longer time aging treatments generally decreased the elongation values excessively.

Lower temperature aging treatments of 8 hr at 280 or 250 F were beneficial, the latter being superior, in that some of the alloys containing 6.0 per cent zinc in addition to those with the higher zinc contents and most of the magnesium contents conformed to the 32,000 psi tensile strength requirement. Increasing the magnesium content to the range of 0.6 to 0.8 per cent and maintaining the zinc in the range of 6.0 to 7.0 per cent afforded tensile strengths of the desired order in specimens aged 4 hr at 220 or 250 F.

The data presented in Fig. 1 disclose the effects of

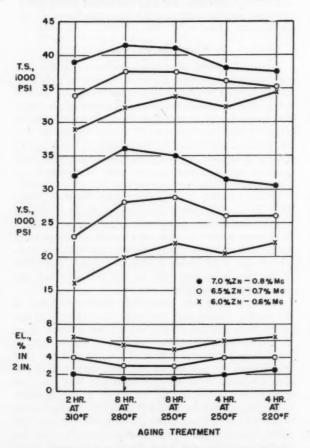


Fig. 1—Effects of various aging treatments on the tensile properties of sand east bars of alloys containing 0.5% Cu, 0.15% Fe, 0.10% Si, 0.15% Ti and the indicated quantities of zinc and magnesium.

various aging treatments on the tensile properties of alloys with zinc and magnesium contents in the selected ranges. Hardness values followed the same pattern as the tensile strengths. It was evident that the best combinations of tensile strength and elongation for these composition ranges were afforded by the aging treatment of 4 hr at 220 F. In addition, the variation in properties with the different zinc and magnesium contents was reduced by use of this aging treatment.

Effect of Iron and Silicon Contents—The effects of iron and silicon on properties are significant because these elements are present as impurities in aluminum and therefore a determination of permissible limits was necessary. Data presented in Fig. 2 show that in-

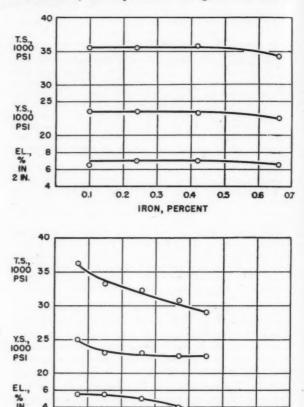


Fig. 2—Effects of variations in iron and silicon contents on the tensile properties of alloys containing 6.5% Zn, 0.7% Mg, 0.5% Cu, 0.15% Ti and 0.07% Si or 0.10% Fe. Sand cast bars aged 4 hr at 220 F.

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creasing the iron content progressively to about 0.5 per cent, in an Al-6.5 per cent Zn-0.7 per cent Mg-0.5 per cent Cu-0.15 per cent Ti alloy containing 0.07 per cent silicon, did not alter the tensile properties and even 0.65 per cent iron had only a slightly adverse effect. However, increasing contents of silicon, in alloys containing 0.10 per cent iron, substantially impaired the tensile strength and elongation values and demonstrated the desirability of maintaining an upper limit

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of 0.15 per cent silicon. These experimental alloys for evaluating the effects of iron and silicon were aged for 4 hr at 220 F. Simultaneously raising the iron and silicon contents reduced the tensile properties to the same extent as would result from increasing only the silicon.

Selected Composition—On the basis of the preceding test results the aluminum sand casting alloy nominally containing 6.5 per cent Zn, 0.7 per cent Mg, 0.5 per cent Cu and 0.15 per cent Ti, identified as alloy A612, was selected for further investigation.

Mechanical and Physical Properties—Minimum tensile properties, for specification purposes, of separately sand cast test specimens of alloy A612 aged for about 5 days at room temperature or 4 hr at 220 F are:

Tensile Strength, psi (min) -32,000 Elongation, % in 2 in. (min) - 3.0

Typical mechanical and physical properties of alloy A612 subjected to room temperature aging for 30 days or to the artificial aging treatment are given in Table 1. It was demonstrated by Eastwood and Kempf that with aluminum-zinc-magnesium alloys of this type the tensile properties of separately cast test specimens are more nearly attainable in castings than is the case with most other high strength aluminum casting alloys.

Aging Characteristics—With a specific magnesium content, increasing the zinc content progressively increases the tensile strength, yield strength and hardness but decreases the elongation. The tensile properties are similarly altered by maintaining a constant zinc content and increasing the magnesium content. Therefore, the data on alloy A612 containing the

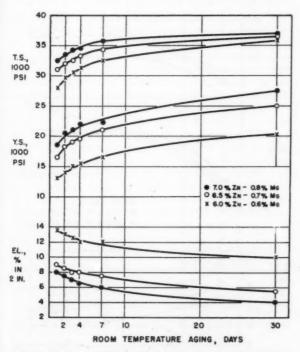


Fig. 3—Effects of variations in room temperature aging on the tensile properties of sand cast bars of alloys containing 0.5% Cu, 0.15% Fe, 0.10% Si, 0.15% Ti, and the indicated quantities of zinc and magnesium.

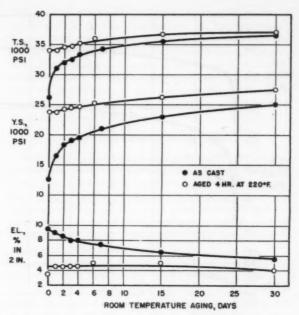


Fig. 4-Representative room temperature aging curves for as-cast and artificially aged alloy A612 containing 6.5% Zn, 0.7% Mg, 0.5% Cu, 0.15% Fe, 0.10% Si, and 0.15% Ti.

TABLE 1-TYPICAL MECHANICAL AND PHYSICAL PROPER-TIES OF ALLOY A612a

| Tensile Strength, psi | 35,000 |
|---|-------------------------|
| Tensile Yield Strength, psib | 25,000 |
| Elongation, % in 2 in. | 5.0 |
| Brinell Hardnesse | 75 |
| Compressive Yield Strength, psid | 25,000 |
| Shearing Strength, psi | 26,000 |
| Endurance Limit, psi' | 8,000 |
| Electrical Conductivity ^e | 35 |
| Thermal Conductivity® | 0.33 |
| Coefficient of Thermal Expansion, per °Fh | 13.4 x 10 ⁻⁶ |
| Specific Gravity, g/cc | 2.81 |

* Aged 30 days at room temperature or 4 hr at 220 F

b Offset = 0.2%

* 500 kg load, 10 mm ball.

⁴ Offset = 0.2%, specimens with 1/r ratio of 12.

R. R. Moore type specimen, 500,000,000 cycles.

At 25 C, per cent of International Annealed Copper Standard.

* At 25 C, in cgs units.

For temperature range 68-212 F.

minimum 6.0 per cent zinc and 0.6 per cent magnesium and the maximum 7.0 per cent zinc and 0.8 per cent magnesium will practically encompass the range of tensile properties attainable with the alloy,

The effect of room temperature aging for periods up to 30 days on the tensile properties of alloy A612 with minimum, nominal and maximum zinc and magnesium contents is shown in Fig. 3. This pattern of initial rapid change and a progressively retarded rate of change in tensile properties with extension of room temperature aging is characteristic of aluminum-zinc-magnesium alloys. The plotted properties of Fig. 4 are those of specimens of nominal composition aged at room temperature in the as-cast condition and after aging for 4 hr at 220 F. It is evident from these data

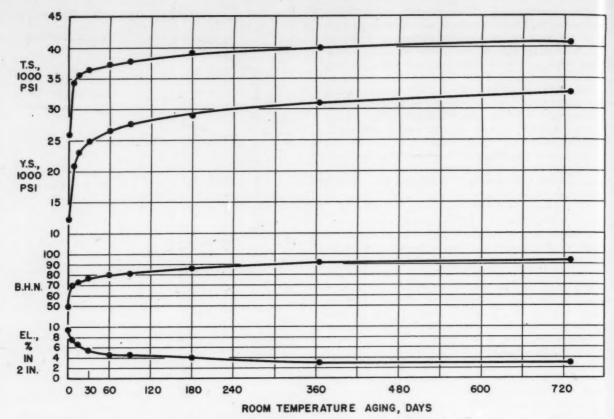
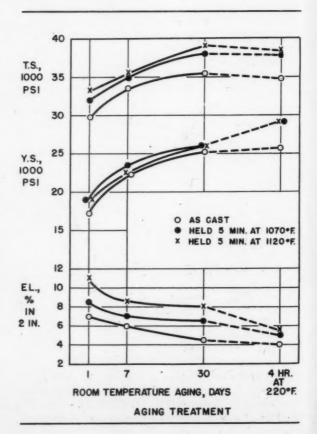


Fig. 5—Representative room temperature aging curves for as-cast alloy A612 containing 6.5% Zn, 0.7% Mg, 0.5% Cu, 0.15% Fe, 0.10% Si, and 0.15% Ti.

that the artificial aging treatment may be employed to avoid the necessity of waiting a few days for attainment of the specified minimum tensile strength. Information on the effect of room temperature aging for time periods up to two years on the tensile properties of nominal composition alloy A612 is supplied in Fig. 5.

Brazing Characteristics—Accumulated data on the aluminum-zinc-magnesium alloys have indicated that alloy A612 should not be considered for eastings expected to function at temperatures in excess of 250 F. However, alloy A612 does possess good brazing characteristics and heating for brazing operations has no adverse effect on tensile properties. Heating at conventional brazing temperatures for short periods of time has the effect of approximately restoring the tensile properties that prevailed immediately after casting. Actually the properties after short periods of room temperature aging, or aging for 4 hr at 220 F, subsequent to heating for 5 min at 1070 or 1120 F were somewhat higher than those of similarly aged as-cast material, as shown in Fig. 6.

Fig. 6—Effects of variations in room temperature aging and an artificial aging treatment on the tensile properties of an alloy containing 6.5% Zn, 0.7% Mg, 0.5% Cu, 0.15% Fe, 0.10% Si, and 0.15% Ti. Sand cast bars in the indicated conditions.



Brazed assemblies of alloy A612 castings and wrought alloy members may have to be subjected to the heat treatment employed for obtaining the —T6 temper in 61S alloy to procure the expected tensile properties in the wrought alloy portion. The effect of heat treating for 4 hr at 970 F, quenching in water at room temperature and aging for 18 hr at 320 F, or 8 hr at 350 F, on the tensile properties of alloy A612 is disclosed in Table 2. Tensile properties after aging for 4 hr at 220 F or for 7 and 30 days at room temperature are included for comparison. It will be noted that a desirable combination of tensile properties was produced with the shorter time and higher temperature aging treatment.

Casting Characteristics—Foundry experience with alloy A612 indicates the necessity for adherence to good practices during melting and handling of the molten metal, in order to minimize the relatively marked tendency for oxidation to occur. Chlorine gas fluxing has proved effective in removing oxide particles from the melt. More generous feeding is required to prevent shrinkage in castings of this alloy than is needed with many commercial aluminum casting alloys. The degree of resistance to hot cracking is similar to that of alloy 195 (Al-4.5 per cent Cu) which limits the intricacy of design in castings that can be produced successfully with A612 alloy.

Resistance to Corrosion—Salt-spray exposure tests, continued for two years, were conducted under standardized conditions² on alloy A612 specimens with minimum, nominal and maximum zinc and magnesium contents. The results showed that all three variations possessed good resistance to this type of corrosion and they were rated as comparable to 43 alloy (Al-5 per cent Si).

Extensive tests have demonstrated that acceleration of corrosion by stress is not encountered in alloy A612 as determined by the standard sodium chloride-hydrogen peroxide test.3 Specimens of alloy A612 and A612 with an addition of 0.5 per cent chromium were immersed in a 5.3 per cent NaCl-0.3 per cent H2O2 aqueous solution. The stress of 19,000 psi, applied to some of the specimens, was approximately 75 per cent of the yield strength values. All specimens, which had previously been aged for 4 hr at 220 F and 2 months at room temperature, withstood the 14 day period of exposure without cracking. Subsequent tensile tests demonstrated that the tensile strength losses with stressed and non-stressed specimens were 13 per cent and 16 per cent respectively for A612 alloy and were 13 per cent and 8 per cent respectively for alloy A612

with added chromium. These observations are in agreement with the conclusion of Eastwood and Kempf that a chromium addition does not affect the resistance to stress-corrosion of aluminum-zinc-magnesium alloys of the compositions investigated. All alloys included in the recent investigation contained 0.5 per cent copper because of the merit of this addition in improving resistance to stress-corrosion, as established by Eastwood and Kempf.

Permanent Mold Casting Alloy

The major objective of this portion of the investigation was the development of an alloy for permanent mold castings which could be used in brazed assemblies, and efforts were directed toward achieving the highest mechanical properties consistent with the requirements of satisfactory permanent mold casting characteristics, good brazeability and commercially acceptable composition limits.

It is generally recognized that the requirement for resistance to hot cracking is more stringent for permanent mold casting than for sand casting. Alloy X612 was found to be unsatisfactory for producing permanent mold castings chiefly because of its tendency to hot crack, and the initial problem in adapting the alloy for casting in permanent molds was to overcome this deficiency without prohibitive sacrifices in tensile properties or brazing characteristics. In maintaining these desirable characteristics it was necessary to limit alloying additions which would lower the solidus temperature and to carefully control elements which accentuate the artificial aging of the alloy.

Effect of Iron Content—A number of variations in composition and additions of elements which might be expected to reduce the hot cracking tendency of the alloy were investigated. The addition of iron was found to be the most effective means of improving the hot cracking resistance. Selection of this element as an intentional addition to the permanent mold alloy was based on laboratory tests and the results of a number of trials in the production foundry. With an addition of about one per cent of iron it was possible to produce certain types of commercial castings with very low rejections for hot cracking or other casting defects whereas hot cracking losses were prohibitive with low iron contents.

Data showing the effects on the tensile properties of permanent mold cast tensile bars resulting from variations in iron content over the range 0.20 to 1.25 per cent are illustrated in Fig. 7. The alloys contained

TABLE 2—COMPARISON OF THE TENSILE PROPERTIES OF HEAT-TREATED AND NATURALLY AGED A612 ALLOY

| Solution Treatment | Aging Treatment | Tensile Strength, psi | Yield Strength, psi | Elongation % in 2 in. | Brinell Hardness Number | | | |
|-----------------------|-----------------------------|-----------------------------|---------------------------|-----------------------|-------------------------------|--|--|--|
| 4 hr at 970 Fa | 18 hr at 320 F | 43,600 | 40,300 | 2.5 | 82 | | | |
| hr at 970 F* | 8 hr at 350 F | 39,900 | 34,500 | 5.0 | 85 | | | |
| None | 4 hr at 220 F | 36,800 | 25,500 | 5.5 | 76 | | | |
| None | 7 days at room temperature | 35,300 | 20,200 | 9.0 | 69 | | | |
| None | 30 days at room temperature | 37,400 | 23,900 | 6.0 | 74 | | | |

^{*}Solution and aging treatments to produce the -T6 temper in 61S alloy, entailing a quench in water at room temperature from solution temperature.

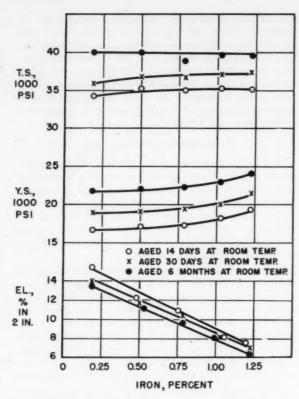


Fig. 7-Effects of variations in iron content on the tensile properties of alloys containing 6.50% Zn, 0.30% Mg, 0.45% Cu, 0.12% Si, and 0.15% Ti. Permanent mold cast bars aged at room temperature for the periods indicated.

6.50 per cent Zn, 0.30 per cent Mg, 0.45 per cent Cu, 0.12 per cent Si and 0.15 per cent Ti, and the tests were conducted after the bars had aged at room temperature for periods of 14 days, 30 days and six months. The tensile strength was not affected by the increased iron contents, the yield strength was slightly increased and the elongation was substantially reduced. Thus, an appreciable sacrifice in the elongation was incurred by the modification employed to reduce the hot cracking tendency. The rate and extent of age hardening were relatively independent of the iron content.

Consideration of the aluminum-iron constitution diagram indicates that the addition of iron to the alloy would produce only a minor reduction in the solidus temperature. Heating the iron-containing alloys in the temperature range employed for furnace or dip brazing had no detrimental effects on the tensile properties. In fact, a small improvement in the elongation and the tensile strength was provided by short time exposures to such temperatures. Figure 8 discloses the results of tensile tests on bars from the preceding series of alloys after heating for 5 min at 1110 and 1130 F followed by air cooling and aging for 7 days at room temperature.

Other tests indicated that the addition of iron has the further advantage of reducing the amount of sagging or warpage of the castings during exposure to brazing temperatures.

Effect of Silicon Content—Silicon additions up to 2.5 per cent were investigated in various modifications of the alloy containing 0.5 to 1.0 per cent iron. Additions of 1.75 per cent or more silicon were required to effect appreciable improvements in the fluidity and feeding characteristics, but at such concentrations the mechanical properties deteriorated, and the permissible brazing temperature was drastically reduced.

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The tensile strength and elongation after aging at room temperature were progressively impaired by increasing silicon contents in the range of 0.1 to 1.0 per cent. Aging at temperatures of 300 to 350 F accentuated these effects, and severe losses in elongation were produced by these treatments at silicon contents in excess of about 0.3 per cent. It was necessary to consider the effects of aging at elevated temperatures since some castings may be used as components of brazed assemblies containing heat-treatable wrought alloy members which require elevated temperature aging for the development of optimum strength.

The effects of silicon on specimens aged at room temperature from the as-cast condition are shown in Fig. 9. The superior elongations and tensile strengths obtained with lower contents of this element led to the selection of a preferred upper limit of 0.20 per cent and a maximum of 0.30 per cent silicon in the alloy.

Effect of Magnesium Content—As has been discussed previously, variations in magnesium content in this type of alloy strongly influence the mechanical proper-

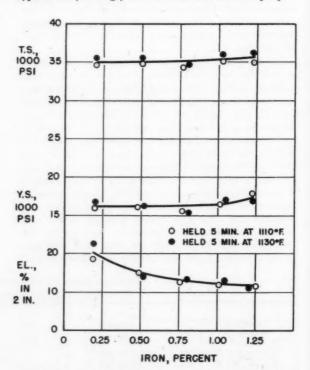


Fig. 8—Effects of variations in iron content on the tensile properties of alloys containing 6.50% Zn, 0.30% Mg, 0.45% Cu, 0.12% Si, and 0.15% Ti. Permanent mold cast bars heated as indicated, cooled in air and aged 7 days at room temperature.

ties. The effects of magnesium variations in the range of 0.2 to 0.8 per cent are shown in Fig. 10. After only room temperature aging the tensile and yield strengths were raised by increasing the magnesium content at the expense of the ductility. These effects were greatly accentuated when a brazing cycle including aging for 8 hr at 350 F was employed, and the loss in ductility with increasing magnesium was marked.

In common with the effect of silicon, increasing the magnesium content lowered the solidus temperature and the permissible brazing temperature. Considering both the reduction in brazing temperature and the embrittling effect after artificial aging caused by increasing magnesium content, the range of 0.25 to 0.45 per cent magnesium was established for the permanent

Effect of Copper Content—Variations in copper content up to about 0.8 per cent had relatively minor effects on the mechanical properties of the alloy. An addition of 0.5 per cent copper was selected for the permanent mold alloy to insure freedom from stress-corrosion cracking.

Selected Composition*—Consideration of the effects of the various elements, individually and in combinations, on the desired charactexistics resulted in selection of an aluminum permanent mold casting alloy, designated C612 alloy, with the nominal composition of 6.5 per cent Zn, 1.0 per cent Fe, 0.5 per cent Cu, 0.35 per cent Mg and 0.10 per cent Ti.

This alloy is the subject of a pending U. S. patent application.

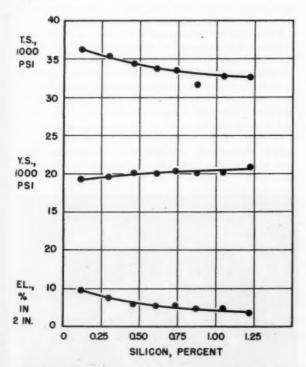


Fig. 9—Effects of variations in silicon content on the tensile properties of alloys containing 6.5% Zn, 1.0% Fe, 0.5% Cu, 0.35% Mg and 0.10% Ti. Permanent mold cast bars aged for 30 days at room temperature.

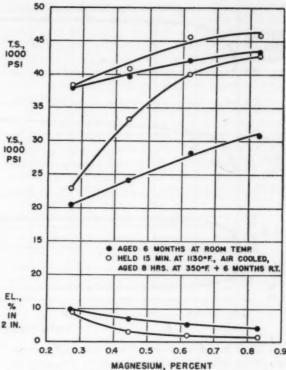


Fig. 10-Effects of variations in magnesium content on tensile properties of alloys containing 6.5% Zn, 1.0% Fe, 0.5% Cu, 0.15% Si and 0.10% Ti. Permanent mold bars in the indicated conditions.

Mechanical and Physical Properties—Minimum tensile properties specified for permanent mold cast alloy C612 test specimens aged for about 5 days at room temperature are:

Tensile Strength, psi (min) –28,000 Elongation, % in 2 in. (min) – 8.0

Typical mechanical and physical properties of permanent mold cast alloy C612 specimens aged 30 days at room temperature are listed in Table 3.

Aging Characteristics—The average changes in mechanical properties with time at room temperature in representative permanent mold cast alloy C612 specimens aged from the as-cast condition are plotted in Fig. 11. It is apparent that rapid age hardening occurs

TABLE 3—TYPICAL MECHANICAL AND PHYSICAL PROPER-TIES OF ALLOY C612a

| Tensile Strength, psi | 35,000 |
|---|-------------------------|
| Tensile Yield Strength, psib | 18,000 |
| Elongation, % in 2 in. | 8.0 |
| Brinell Hardness ^e | 70 |
| Compressive Yield Strength, psid | 18,000 |
| Shearing Strength, psi | 22,000 |
| Electrical Conductivity® | 40 |
| Thermal Conductivity ^t | 0.38 |
| Coefficient of Thermal Expansion, per °Fe | 13.1 x 10 ⁻⁶ |
| Specific Gravity, g/cc | 2.84 |
| 1 1 00 1 | |

* Aged 30 days at room temperature.

b Offset = 0.2%

° 500 kg load 10 mm ball.

^d Offset = 0.2%, specimens with 1/r ratio of 12.

At 25 C, per cent of International Annealed Copper Standard.

At 25 C, in cgs units.

* For temperature range 68-212 F

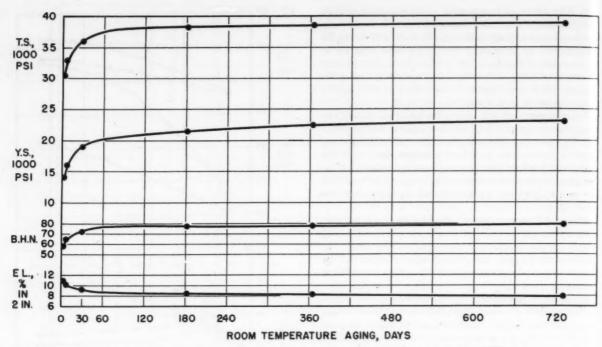


Fig. 11-Representative room temperature aging curves for as-cast alloy C612 containing 6.5% Zn, 1.0% Fe, 0.5% Cu, 0.35% Mg, 0.20% Si and 0.10% Ti.

in the first few months but that relatively little further change takes place after about six months.

Short time elevated temperature aging treatments do not completely stabilize the mechanical properties, and further changes occur at room temperature after such aging treatments. The specific behavior is dependent to a considerable degree upon the magnesium and silicon contents as well as the time and temperature, and under some conditions artificial aging may be permanently damaging to the properties. Aging at temperatures of 300 F or below provides relatively little initial hardening and retards subsequent natural aging. An initial increase in the yield strength, little change in the tensile strength, and reduced elongation result from heating at 350 F. Room temperature aging subsequent to treatment at 350 F provides additional hardening.

Losses in elongation, incurred through elevated temperature aging of the type that may be applied to brazed assemblies, were minimized by selection of the alloy C612 composition limits. As a result of the lower magnesium content of alloy C612, an artificial aging treatment of the type recommended for alloy A612 is not effective with the permanent mold alloy.

Brazing Characteristics—Brazed joints having excellent appearance and mechanical strength may be produced readily with two or more permanent mold castings of C612 alloy or with these castings and wrought parts of appropriate alloys by furnace or dip brazing, employing conventional techniques. The castings exhibit very good resistance to excessive penetration by the filler metal, absence of deterioration by internal eutectic melting and resistance to sagging or warpage

at brazing temperatures up to 1120 F.

The tensile properties of C612 alloy after a brazing operation performed at 1090 to 1120 F followed by cooling in air to room temperature, or air cooling to 960 F and then quenching in water, are comparable to those that exist immediately after casting. Subsequent response to room temperature aging after such heating is the same as with as-cast material.

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When the brazing operation is followed by an artificial aging treatment of 8 hr at 350 F, the yield strength is raised and the elongation reduced. After brazing, artificial aging at 350 F, and 30 days aging at room temperature, the typical mechanical properties are: Tensile Strength—36,000 psi, Tensile Yield Strength—24,000 psi, Elongation—5.0 per cent in 2 in., and Brinell Hardness—75.

Casting Characteristics—The variety of castings produced commercially in C612 alloy has not been adequate to provide detailed information on the com-



Fig. 12-Bus torque converter components fabricated of cast and wrought aluminum alloy parts by brazing.

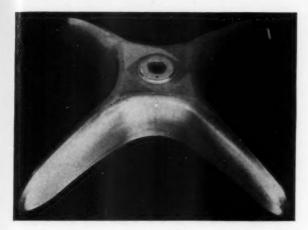


Fig. 13-Swivel chair base fabricated by brazing aluminum alloy castings and sheet.

parative casting characteristics, nor to closely define the foundry limitations of the alloy. The fluidity of the alloy permits casting of sections as thin as ½ in. in relatively small parts, but somewhat greater section thicknesses may be required in larger castings. The shrinkage and feeding characteristics have not presented any particular difficulties, although there are indications that the gating and risering practices may require more consideration than is necessary with most permanent mold alloys. The alloy is somewhat more susceptible to hot cracking than many of the permanent mold alloys, and generous fillet radii and careful blending of sections are desirable.

Resistance to Corrosion—Exposure for periods up to two years in intermittent and continuous salt spray² have indicated that permanent mold castings of C612 alloy are comparable to castings of 43 alloy (Al-5 per cent Si) in resistance to corrosion.

Permanent mold cast tensile bars successfully withstood accelerated stress corrosion tests³ in which tensile stresses up to 18,000 psi were employed with continuous immersion in a 5.3 per cent NaCl + 0.3 per cent H_2O_2 solution at room temperature for 14 days. These tests were made on specimens which had been aged at room temperature for periods up to 13 months as well as others which were heated to temperatures in the brazing range followed by artificial aging and additional room temperature aging for periods up to seven months.

Applications

The good combinations of tensile properties provided by alloys A612 and C612 without heat treatment suggest fields for application. These alloys also possess excellent brazing characteristics and this property has been utilized in production of parts, some of which merit description.

Three components of a bus torque converter, shown in Fig. 12, are being produced as brazed assemblies. The blades in all components are of permanent mold cast C612 alloy. In the pump member, at the left, the body and recessed ring were alloy A612 sand castings during the development stage but both are now

wrought aluminum alloy parts. Cast blades and wrought aluminum alloy parts are employed in production of the turbine member, at the right. The reaction member, in the center of the photograph, was produced as a one-piece casting but this member is currently fabricated by brazing permanent mold cast C612 alloy blades to a hub and outer ring of sand cast A612 alloy.

Another production use for permanent mold castings of alloy C612 has been in swivel chair bases. Figure 13 shows the finished chair base in the normal position, and the inverted view of Fig. 14 reveals construction details. Alloy C612 center hub and caster holder castings, the latter made with cast-in steel inserts, are assembled with aluminum alloy sheet by furnace brazing.

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3. É. H. Dix, Jr., "Acceleration of the Rate of Corrosion by High Constant Stresses," *Transactions*, Institute of Metals Division, AIMME, vol. 137, p. 11 (1940).

DISCUSSION

Chairman: R. E. WARD, Eclipse-Pioneer Div., Bendix Aviation Corp., Teterboro, N. J.

Co-Chairman: W. E. CARVER, Light Metals, Inc., Indianapolis,

HIRAM BROWN (Written Discussion): I am particularly pleased to have read this very interesting paper by Messrs. Sicha and Hunsicker since it adds to the many nice things that have been said in recent years concerning aluminum-zine-magnesium alloys. Also, in the paper I recognized old friends in new clothes. By this I mean that the alloys in this paper are, for all practical purposes, the Frontier 40 alloys, although they are labeled as 612 alloys.

¹ Chief Metallurgist, Solar Aircraft Co., Des Moines, Iowa.

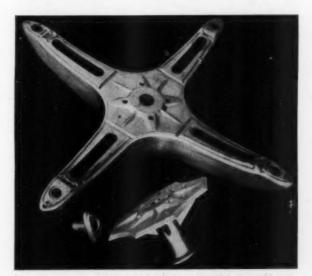
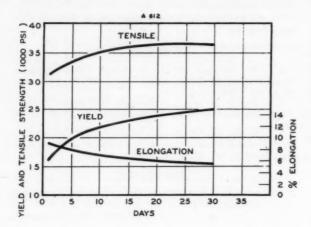


Fig. 14—Chair base in inverted position showing locations in the brazed assembly of center hub and caster holder castings.

TABLE A-Typical Physical Properties of 40E and A612

| | All | oy |
|----------------------------------|-----------|---------|
| | 40E | A612 |
| Tensile Strength, psi | 35,000 | 35,000 |
| Yield Strength, psi | 25,000 | 25,000 |
| Elongation, % in 2 in. | 5 | 5 |
| Shear Strength, psi | 26,000 | 26,000 |
| Brinell Hardness, BHN | 75 | 75 |
| Endurance Limit | 8,000 | 9,000 |
| Coefficient of Thermal Expansion | 13.4×10-6 | 13.7x10 |
| Max Operating Temp, F | 280 | 250 |
| Max Brazing Temp, F | 1100 | 1120 |



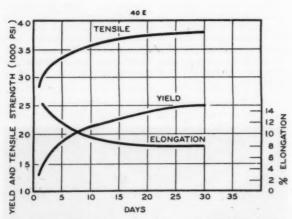


Fig. A-Room temperature aging characteristics of A612 and 40E.

Table A shows the striking similarity between nine physical properties listed for 40E and A612 respectively.

Further similarity is shown by comparison of the room temperature aging curves of A612 and 40E shown in Fig. A. The curves of the two alloys could not be plotted on the same chart because of the almost exact overlap of the respective curves.

As far back as May 1939, Alcoa had analyzed and tested parts made from Frontier 40 alloy and at that same time worked on an alloy called X612 which was "designed to compete with or replace Frontier 40."

Then, in 1941, Eastwood and Kempf² extensively investigated aluminum-zinc-magnesium alloys and came to the conclusion that the alloy referred to in this paper as EX alloy was the one which appeared to have the maximum combination of strength, ductility, and resistance to corrosion. Now in 1950 alloy A612 is presented. The similarity between X612, A612, EX alloy and 40E is shown in Table B.

TABLE B-COMPARATIVE ANALYSIS OF FRONTIER 40E AND 612 ALLOYS

| | | | | | |
|--------------------|------|---|------|-------------|------------------|
| | X61 | 2 | EX1 | A612 | 40E ² |
| Zinc | 6.5 | 3 | 6.6 | 6.5 | 5.0-6.0 |
| Magnesium | 0.2 | 4 | 0.33 | 0.7 | 0.5-0.65 |
| Chromium | 0.2 | 4 | 0.25 | _ | 0.4 - 0.6 |
| Titanium | 0.13 | 3 | 0.15 | 0.15 | 0.10-0.30 |
| Copper | 0.47 | 7 | 0.4 | 0.5 | 0.3* |
| Silicon | 0.13 | 3 | 0.08 | - Committee | 0.3* |
| Iron | 0.22 | 2 | 0.15 | | 1.0* |
| Manganese • May | - | - | _ | _ | 0.3* |

1. L. W. Eastwood and L. W. Kempt, "Aluminum-Zinc-Magnesium-Copper Casting Alloys," Transactions, A.F.S., vol. 56, pp. 100-115 (1948).

2. ASTM, B26-48T, Alloy ZC61A

On page 335 of the paper by Messrs. Sicha and Hunsicker appears the statement "Increased zinc progressively increases tensile strength, yield strength, and hardness, but decreases elongation. Tensile properties are similarly altered by maintaining a constant zinc content and increasing magnesium." This statement has been proven a number of times previously, particularly by Frontier 40 alloys whose patent⁸ covers the range of 5 to 10 per cent zinc, and 2.5 to 0.3 magnesium. Furthermore, a curve and formula clearly expressing this relationship was developed by Brown and Holzworth and patented by Canadian Patent 441,793. The equation and curves are shown in Fig. B.

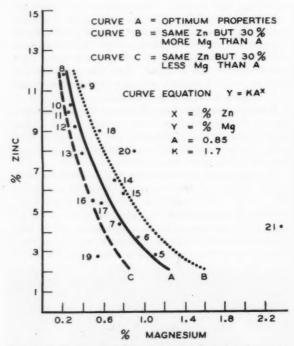


Fig. B-Zinc-magnesium ratios. All zinc and magnesium ratios of curves A, B, and C will give tensile strength in excess of 30,000 psi and elongation in excess of 3% in 2 in.

Briefly, they indicate that mathematically calculated portions of zinc and magnesium according to the formula, or derived from the curves, will give alloys which will meet a definitely established value of tensile strength, yield strength, and elongation. All of the aluminum-zinc-magnesium alloys shown in Table B—including A612—fall within this curve. As a matter of fact, point 14 on the curve is the exact zinc and magnesium content as that shown in A612 analysis.

On page 337 of Messrs. Sicha and Hunsicker's paper appears

the statement "All alloys included in the recent investigation contained 0.5 per cent copper because of the merit of this addition in improving resistance to stress corrosion as established by Eastwood and Kempf." 1 And on page 339 appears the statement, "An addition of 0.5 per cent copper was selected for the permanent mold alloy to ensure freedom from stress corrosion cracking." To those not familiar with the aluminumzinc-magnesium alloys, this may give the erroneous impression that such alloys without copper will be subject to stress corrosion cracking. This is not so. There is nothing to indicate stress corrosion troubles in aluminum-zinc-magnesium alloys until copper is added. Actually, Eastwood and Kempf salt spray tested only four alloys, all of which contained at least 0.35 per cent copper. Furthermore, Eastwood and Kempf1 reported that, "It has been found that bars stressed at 75 per cent or less of the yield strength are not subject to intergranular corrosion when continuously immersed in a solution of sodium chloride and hydrogen peroxide provided the zinc does not exceed about 7 per cent and the copper is not less than 0.25 per cent or more than 0.6 per cent." And further, they state that, "The alloy containing I per cent copper appeared inferior in corrosion resistance to that containing 0.35 per cent copper." This indicates copper limits must be controlled. Since 40E permits copper up to 0.3 per cent, this leaves only the range of 0.3 to 0.6 per cent copper for A612 alloy to use without increased corrosion taking place. As a matter of fact, there is reason to believe that in accelerated sodium chloride-hydrogen peroxide tests, the optimum combination for high strength and maximum resistance to stress corrosion occurs when zinc is 5.5 to 6 per cent and copper 0.3 to 0.5 per cent. This leaves a range of only 0.2 per cent copper between the 0.3 per cent copper maximum of 40E and the 0.5 per cent copper figure just mentioned. This is quite a narrow range.

Although the findings of Eastwood and Kempf¹ did show that added chromium had no beneficial effect on the resistance to stress corrosion in the NaCl-H2O2 cell, chromium does improve resistance to corrosion by salt spray. In view of this fact it certainly seems that nothing is gained by omitting chromium from the alloy.

It appears that due to the accent placed by the authors on the so-called improvements due to copper, and the supposedly neutral effect of chromium, this might indicate that presence of copper and absence of chromium may be merely fine technical points to circumvent 40E composition.

It was pointed out on page 334 of the paper that, "Increasing silicon in alloys containing 0.10 per cent iron substantially impaired the tensile strength and elongation values and demonstrated the desirability of maintaining an upper limit of 0.15 per cent silicon." While ranges of composition were not given it would be interesting to see whether chemical limits have had to be set rather close in order to obtain the properties which can be obtained from 40E with perhaps a wider spread in permissible composition. It would be my guess that copper, silicon, and iron would have to be controlled closer than in 40E if satisfactory elongation is to be maintained. It would appear throughout the paper that the authors are much less concerned about low ductility than were Eastwood and Kempf.1

It should, however, be encouraging to foundrymen to find that aluminum-zinc-magnesium alloys are good alloys for many purposes, and that many prejudices against alloys of this type can be thoroughly dismissed from their minds.

REFERENCES

1. L. W. Eastwood and L. W. Kempf, "Aluminum-Zinc-Magnesium-Copper Casting Alloys," Transactions, A.F.S., vol. 56, pp. 100-115 (1948)

2. U. S. Patent 2,146,330, to George F. Comstock, Feb. 7, 1939. Application Feb. 18, 1937

MR. SICHA: Mr. Brown's remarks are interesting and we appreciate his courtesy in giving us an opportunity to review his comments prior to their presentation at the Convention.

There are several commercial aluminum-zinc alloys, some of which Mr. Brown did not mention, and they all belong to the same family. They are all aluminum-zinc-magnesium alloys basically. The written discussion has not given recognition to the fact that these alloys resulted from three investigations that were in progress simultaneously. No two of these alloys are

identical in composition and each producer has arguments to justify his selection of the specific composition which he favors. It is only natural that, as Mr. Brown pointed out, many of the characteristics of the individual alloys will be similar.

It is our contention that A612 alloy ages more rapidly at room temperature in the early stages because of the higher magnesium and zinc contents. This composition also affords an advantageous response to artificial aging which aids in establishing tensile property specifications. Alloy C612 is distinctive in that it provides definitely improved characteristics for the production of permanent mold castings.

With reference to the copper addition, the significant point is that in A612 and C612 alloys the addition of a half per cent copper provided improvement in resistance to corrosion under stress. This advantage was demonstrated by means of accelerated laboratory tests.

It was stated by Eastwood and Kempf, and has been confirmed in the work on A612 and C612, that chromium additions were not beneficial in the sand cast or permanent mold casting alloys. In Mr. E. H. Dix's Memorial Lecture, the merit of a chromium addition in wrought Al-Zn-Mg alloys was described, but it was pointed out that these remarks did not apply to the cast Al-Zn-Mg alloys. There was no justification for increasing the cost by adding chromium because tests on X612, A612 and C612 showed that chromium additions had no significant effect on resistance to stress corrosion cracking or salt spray corrosion.

J. J. WARGA (Written Discussion): 3 For the past 11/2 yr we have been conducting laboratory and foundry tests on the available commercial alloys of this group. It may be of interest to comment on our findings with respect to A612. The following properties were evaluated: fluidity and castability, reaction to brazing, corrosion and stress-corrosion, and mechanical strength as sand-cast and as investment-cast.

Fluidity and castability of A612 sand-cast was the poorest of the alloys tested. Our tests were performed using the modified fluidity spiral proposed by Sicha* and the average weight in the spirals was found to be 1.86 oz. The pouring temperature was 1375 F. Castability tests conducted using the casting shown in Fig. C confirmed the results of the fluidity tests.

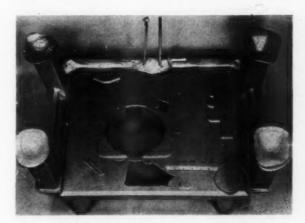


Fig. C-Alloy XA-612, castability sand casting.

Brazing of this alloy was accomplished without difficulty. In our procedures, an investment cast mount was brazed to 61S-T waveguide tubing in a flux bath operating at 1120 F. After brazing, the brazed joint was sectioned and the extent of fill of the brazing alloy and the microstructure of the A612 were examined. The fill was good, and the A612 was free of eutectic

To check the corrosion resistance, samples were exposed to 20

^a W. E. Sicha and R. C. Boehm, "A Fluidity Test for Aluminum Casting Alloys," Transactions, A.F.S., vol. 56, pp. 502-507 (1948). ^a Standards Engineer, Materials and Processes, Sperry Gyroscope Co.,

Great Neck, Long Island, N. Y.

per cent salt spray as-cast, anodized in sulphuric acid, and anodized in chromic acid. Comparison of the anodized surfaces after 500 hr leads us to conclude that while satisfactory, it is not the most resistant of this class, its resistance in the as-cast condition being exceeded by two other alloys; the as-cast specimens failed at the end of 24 hr. Stress-corrosion cracking did not occur in specimens loaded as simple beams and exposed to 20 per cent salt spray at a maximum fiber stress of 66 per cent of the yield strength. All other alloys of this group withstood the same conditions.

When sand cast and aged at 215 to 225 F for 4 hr, tensile test bars of A612 had the following average properties:

0.2% Yield Strength-31,350 psi

Ultimate Tensile Strength-35,000 psi

Elongation in 2 in.-2.0%

Impact Strength (Charpy Keyhole)-9 in.-lb.

When investment-cast and aged as above, subsize tensile specimens yielded the following results:

0.2% Yield Strength—28,500 psi Ultimate Tensile Strength—31,300 psi

Elongation in 1.4 in.-1.2%.

The mechanical properties, except for impact, were higher than any of the other alloys tested.

MR. Sicha: I was very much interested in the reported castability relationship. Development of a generally satisfactory specimen for evaluating castability would be an important contribution to the foundry industry. Further information on the test specimen used by Dr. Warga would be welcomed.

The tensile properties reported raise a question regarding quality of the test specimens. The low elongation values suggest that the test bars may not have been completely sound. If they were not as sound as should be produced, it undoubtedly would be reflected in the resistance to corrosion.

We are convinced from our own experiments with salt spray testing, either continuous or intermittent, that the resistance to corrosion of both of these alloys is comparable to that of 43 alloy, which you know ranks among the best of the aluminum casting alloys.

Mr. Holzworth's Discussion

E. H. Holzworth (Written Discussion): The authors are to be complimented on this paper because there is no doubt that considerable effort was consumed in the investigation of this allow.

Since 1937, when aluminum-magnesium-zinc-titanium-chromium alloys were discovered to have unusual physical properties in the as-cast state when aged at room temperature for 20 to 30 days (Alloy 40E, ASTM, B26-ZG61a), Frontier Bronze has had almost a continuous investigation as to the effect of the relative ratios of zinc and magnesium as well as the effect of additions of such elements as copper, manganese, iron, silicon, lead, tin, and many others. During this period of time many thousands of test bars were examined. This statement is made so as to justify our right to discuss the above mentioned paper.

Experimenting throughout the years we have found no beneficial effect from the use of copper, manganese, or silicon, from the standpoint of a combination of physical properties, impact or corrosion. Copper, manganese and silicon have a tendency to embrittle the alloy, manganese having little detrimental effect up to 0.4 per cent. Silicon is detrimental to the alloy in any percentage. Relatively high copper with high silicon are very detrimental. Because of this, the total amount of copper, manganese, and silicon is limited to 0.75 per cent and that of silicon alone to 0.25 per cent. Many attempts were made during World War II to increase these percentages so that scrap containing these elements could be used and some 500,000 lb were made with 0.4 copper, 0.4 manganese, and 0.3 silicon but was discontinued because of the failure to obtain uniformly desirable physical properties. It is relatively easy to obtain tensile strengths of 45-50,000 psi by sacrificing elongation.

With a constant percentage of zinc and raising the magnesium, the tensile strength is increased and elongation decreased. Conversely, lowering the magnesium decreases tensile strength and yield and increases elongation. The zinc-magnesium ratio is very sensitive and small amounts of magnesium alter the physical

In the alloy under discussion, the chromium is eliminated and replaced by copper, but we have been unable to find any bona-fide reason for this substitution, nor does this paper disclose any. Nor, does this paper disclose the advantage of a small percentage of copper over the same composition without the copper.

The impression is given that artificial aging impairs physical properties. This may be true when the chromium is replaced by copper, but we have found no difficulty in meeting the physical properties by artificially aging the copper-free chromium-bearing alloys. Millions of pounds have been artificially aged.

We do agree with the statements of the authors in regard to the brazing characteristics, but the same thing is true of the present ASTM Alloy as shown by the following table.

EFFECT OF EXPOSURE TO BRAZING TEMPERATURES AND Re-AGING AT ROOM TEMPERATURE
ALLOY-ASTM B26-ZG61a

| Treatment | | Tensile Strength, psi | | BHN |
|-----------------------------------|--------|-----------------------------|------|-----|
| 31 days 85 F | 20,300 | 35,600 | 10.5 | 66 |
| 26 days 85 F + 2 hr at 900 F | | | | |
| + 30 days 85 F | 19,500 | 33,600 | 9.0 | 64 |
| 26 days 85 F + 2 hr at 950 F | | | | |
| + 30 days 85 F | 19,500 | 33,450 | 9.0 | 63 |
| 26 days 85 F $+$ 2 hr at 1000 F | | | | |
| + 30 days 85 F | 19,500 | 32,700 | 8.0 | 67 |
| 26 days 85 F $+$ 2 hr at 1050 F | | | | |
| + 30 days 85 F | 19,600 | 34,650 | 11.0 | 68 |
| 26 days $85 F + 2 hr$ at $1100 F$ | | | | |
| + 30 days 85 F | 19,900 | 35,800 | 12.9 | 67 |
| 26 days $85 F + 2 hr at 1200 F$ | | | | |
| + 30 days 85 F | 19,300 | 35,200 | 13.1 | 66 |

With regard to the resistance to corrosion, the statement is made that "These observations are in agreement with the conclusions of Eastwood and Kempf, that a chromium addition does not effect the resistance to stress corrosion of aluminum-zincmagnesium alloys of the compositions investigated."

According to our interpretation of the paper entitled "Aluminum-Zinc-Magnesium-Copper Casting Alloys" by Eastwood and Kempf,¹ page 7, Conclusion 1, this paper definitely states, "The resistance of the alloys containing 0.25 per cent chromium is superior to that of the chromium-free alloys." The findings in the paper under discussion also do not offer any evidence to the support of the statement that chromium has no effect. It is true that you state that all specimens withstood the 14-day period of exposure, without cracking, but you continue and say that subsequent tensile tests demonstrate that the tensile losses with stressed specimens and unstressed specimens were 13 and 16 per cent respectively for alloy A612, and were 13 and 8 per cent respectively for alloy A612 with added chromium. The advantage by this statement is with the alloy containing chromium.

Realizing that this paper deals with castings and the following statements were made in regard to wrought material, we cannot help but feel that it would still hold true when applied to castings. E. H. Dix, Jr., in his Campbell Memorial Lecture in 1949 states that Nock, in his efforts to improve the stress corrosion resistance of aluminum-zine-magnesium alloys, found that small additions of chromium were effective in increasing the resistance to stress corrosion. Speaking directly of alloy 75S, the author states, page 1121, Transactions, ASM 1950, "The impression may have been gained that chromium completely eliminates the susceptibility of stress corrosion cracking of the alloy. This is not true, but chromium does improve the resistance to stress corrosion so that the degree of susceptibility is comparable to that of other high-strength aluminum products which have given long

³ President, Frontier Bronze Corp., Niagara Falls, N. Y.

^{*} EDITOR'S NOTE: This statement appeared in A.F.S. preprint 48-12 but it was corrected by the author before publishing in the Transactions. See p. 106, vol. 56.

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and satisfactory service in important engineering structures."

The Aluminum Company of America, in their Handbook of 1947 states that alloys in which substantial percentages of copper are used are more susceptible to corrosive action. "Freedom from susceptibility to stress corrosion cracking, which for years prevented commercial exploitation of alloys of this class containing zinc, magnesium, and copper, results from the proper choice of the proportions of these elements and the additions of chromium, which serves as a stabilizing agent."

In conclusion, we would say that there is nothing new in this alloy for sand castings, nor are there any combination of properties superior to those already disclosed in the present ASTM alloy, B26-ZG61a. It merely states what can be done by the substitution of copper for the chromium and we cannot agree with their statement that chromium does not effect the resistance to stress corrosion of aluminum-zinc-magnesium alloys, with or without copper. The paper does not disclose the advantage of copper additions to this alloy as compared to the same composition but without copper being present.

MR. Sicha: Many of Mr. Holzworth's remarks are entirely in agreement with our findings. The whole point of difference centers in the relative merits of copper and chromium additions, and many of the things I might say on the subject would be repetition of material in the paper.

However, I would like to comment on an item in Mr. Holzworth's discussion regarding the paper by Eastwood and Kempf. As was disclosed in the A.F.S. Transactions, vol. 56, p. 106 (1948), the work of Eastwood and Kempf showed that the resistance to corrosion of X612 alloy was of the same order with 0.25 per cent chromium added or omitted. Also, the marked influence of about 0.4 per cent copper as an aid in improving resistance to stress corrosion cracking was covered in the discussion section of the Eastwood and Kempf paper.

With regard to Mr. Holzworth's question on the tensile strength losses on A612 with or without 0.5 per cent chromium, the two compositions would be considered as equivalent in resistance to stress-corrosion cracking. The differences in the quoted percentage loss figures were too small to be considered significant, but the small difference that existed favored the alloy without chromium.

WALTER BONSACK: 4 We have in this paper and discussion thereof a background on the family of aluminum-zinc-magnesium alloys. Quite a few people have been busy in this family trying to create new strains. I have been busy too and I may be called a father of a series of alloys in it. We have also heard some arguments about this family tree so I would like to go back a little into history as I know it and see whether we can agree on the grandfather.

Around 1923 Guertler and Sanders experimented with the first Al-Mg-Zn alloys which use the zinc-magnesium compound MgZn₂ as the hardening and strengthening element. They actually developed a series of alloys for castings which constitute the basis for the alloys discussed herein. From then on, other people took up these alloys and added to them minor alloying elements. Some people went in one direction—some in another in an endeavor to add to the high quality of the alloys, either to make them still stronger, or easier to cast, or to enhance the corrosion resistance. Although the basic alloys are the same, they differ slightly in the minor constituents and with them may also differ in some properties.

We developed, as stated above, in the middle '30's at the National Smelting Co. another series of alloys within this family but with a lower zinc content, and a higher magnesium content, than the alloys discussed here. Even within the newer branch of

alloys we were able to develop different traits. Although their family name is the same, their characteristics vary from alloy to alloy. These alloys, just like the alloys discussed here, have the desirable characteristics of developing at room temperature high strength and ductility without having to resort to heat treatment.

MR. HOLZWORTH: In the promotion of the use of zincmagnesium alloys, I understand you have gone back pretty well into the history of it. It seems to me, as I remember, that zincmagnesium alloys were used or attempted to be used for a good many years prior to about 1937, but they were never used to any extent until after the zinc-magnesium-chromium-titanium alloy with minimum amounts of copper, manganese and silicon was developed. It was developed at that time and that was the beginning of the extensive use of the zinc-magnesium-chromiumtitanium alloy in this country.

MR. Bonsack: That is correct. As I recall, about in 1923 Sanders and Guertler brought out the original Al-Zn alloys and about 1928 or 1930 Fuller and Basch of General Electric Co. brought out a similar series of alloys, but they never took hold in this country until 40E was promoted on a high scale. I think it is very much to the credit of Mr. Holzworth and his group to promote these alloys and point out to the public the excellent characteristics of these alloys.

It is important to establish once and for all that we know the history of these alloys and go on from there and do some constructive work.

R. T. PARKER: ⁵ My question concerns the addition of iron to get an alloy with hot cracking properties. I must confess that I had the impression that the addition of iron would leave us with a structure which is intrinsically rather brittle. Is the cracking the authors experienced true hot cracking?

Furthermore, if the iron is decreased, would the alloy be more brittle? Also, can the authors give us any information on the relative impact properties of the base, A612 and C612, as regard to one with the other and compared to different alloys?

MR. HUNSICKER: The cracking experienced in casting the lowiron content alloys in permanent molds is believed to be of the type usually designated as hot cracking, that is, cracking which occurs at temperatures above the solidus temperature of the alloy. It is distinguished from mechanical cracking which may occur a sub-solidus temperatures as a result of tensile stresses developed during contraction of the alloys in the solid state. The hot cracking was normally associated with some shrinkage porosity, and was found in localized areas in which solidification was somewhat retarded compared with that of surrounding areas.

The alloy X612 with low-iron content solidifies essentially as a solid solution. The addition of iron to the alloy introduces a substantial amount of constituent of the type termed "binary complex" in which two components, the aluminum-rich solid solution and the intermetallic phase alpha (Al-Fe), solidify simultaneously. The presence of increasing amounts of "binary complex" as well as eutectic in aluminum alloys is usually associated with improved feeding characteristics and reduced hot cracking tendency. It is believed that the reduced susceptibility to hot cracking exhibited by the iron-containing alloy is attributable to the change in mode of solidification.

Mr. Sicha: We have not accumulated impact data on these alloys because in our opinion the values have very little significance. Attempts to compare Charpy or Izod impact data on dissimilar metals can be very misleading with respect to casting serviceability.

^{*} E. E. Stonebrook and W. E. Sicha, "Correlation of Cooling Curve Data with Casting Characteristics of Aluminum Alloys," Transactions, A.F.S., vol. 57, pp. 489-96 (1949).

⁵ Assistant Director of Research, Aluminum Laboratories Ltd., Banbury, Oxon, England.

Vice President and Director of Research, Christiansen Corp., Chicago.

OXYGEN CUTTING PROCESSES IN STEEL FOUNDRIES

By

R. S. Babcock*

ABSTRACT

In the early days of oxygen cutting, the process found limited application in steel foundries for removing risers and gates from carbon steel castings. As new phases of oxygen cutting developed, it was found that these processes could be applied to steel foundry work as means of increasing production and reducing processing costs. In addition to the basic use of the hand blowpipe on the cleaning floor for rapid removal of risers and gates, use of oxygen has spread to machine cutting for removing excess material, lancing as a means of removing risers and heads from extremely large castings, gouging in defect removal, and scarfing for pad reduction.

More recently the introduction of powder cutting and associated processes has provided a means of overcoming the oxidation resistance of the stainless steels so that castings of these materials may be cleaned and conditioned by oxygen processes as readily as carbon steel castings. It has also been found that the action of the powder gouging process can be used in removing burned sand from fillets and larger areas of sand inclusions in carbon and stainless steel castings.

This paper describes the latest developments in apparatus and operating techniques pertaining to the above-mentioned processes. Many examples and photographs of production application are included.

THE FOUNDRY INDUSTRY was already established by hundreds of years when the oxygen-cutting process was conceived as a means of severing iron and steel at the end of the 19th century. Progress in steel foundries since that time has been closely allied with the development of oxygen-cutting apparatus and the various oxygen-cutting processes. Use of oxygen in steel foundries has increased to such proportions today that it can be considered one of the major consumable items in the manufacture of steel castings. Its usage has resulted in improvement in casting production and quality as well as reduction in processing costs. Oxygen processes have been instrumental also in making foundries better places in which to work by reducing the drudgery and lowering the noise level on cleaning floors. While oxygen-cutting is primarily a cleaning floor operation, the influence of these processes can be seen in production planning, on molding benches, pouring floors, and casting-conditioning areas.

Use of oxygen in steel foundries prior to the first World War was limited to the severing of risers, gates, and excess material from steel castings. Today steel foundries are using oxygen through manually and mechanically operated cutting blowpipes and nozzles of conventional design for rough cleaning of castings; for defect removal by gouging; for pad reduction by scarfing; and for trimming operations. Large-capacity machine-cutting equipment is frequently employed for special severing operations. Oxygen lancing and, more recently, the new powder-cutting process are being used for severing oxidation-resistant sections of sand-incrusted carbon steel, stainless steels, cast iron, and slag-included materials.

As with many operations, an oxygen-cutting process may be accepted in one shop because it results in reducing processing costs. Its adoption elsewhere may be for other reasons such as increased production, improved operating conditions, or improved quality of products. Although many of these processes brought about increases in the total gas consumption, by eliminating other processes, they thereby reduced the total processing cost per ton to a level below that estab-

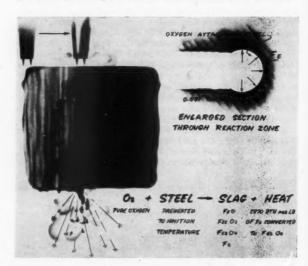


Fig. 1-Oxygen-Cutting Reaction

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Development Engineer, The Linde Air Products Co., Newark, N. J.



Fig. 2-Cutting Bench for Small Castings



Fig. 3-Cutting Risers From Small Castings

lished prior to the introduction of the oxygen process. Naturally, a cleaning operation requiring an increase in gas and labor amounting to \$2.00 per ton can be easily justified if a finishing operation costing \$4.00 per ton can be eliminated.

Oxygen-Cutting Process

Pure oxygen has a high affinity for iron, particularly when the metal is heated to the ignition temperature. This combustion forms a liquid iron oxide which is accompanied by the liberation of a large quantity of heat. The principal factors involved in oxygen-cutting are shown in Fig. 1. In the oxygen-cutting reaction a jet of oxygen impinging upon a section of iron heated to the ignition temperature by the preheat flame creates a slot or kerf. The progress of the kerf is easily controlled by the operator. The size of this local reaction, the thickness of metal it will cut, and

the speed at which it advances depend upon the size and the velocity of the oxygen stream. Each nozzle size is most efficient for a limited range of metal thickness. Speed of the local reaction also depends upon the temperature of the material and the nature and amount of the alloying elements which are present.

The presence of carbon in iron, as in low- or medium-carbon steels, facilitates the oxygen-cutting reaction. Further addition of carbon retards the reaction rate. Addition of nickel and chromium also retards the reaction rate. The oxidation resistance of the cast iron and stainless steels are so pronounced that special techniques are required to sever these materials by oxygen-cutting. The cooling action of the surrounding base metal causes the kerf surface to harden in proportion to the hardenability of the material. The more critical air-hardening or alloy steels may require preheating or subsequent annealing to prevent cracking and to soften the surface for machining. This hardening effect is less pronounced when cutting operations are carried out on castings before allowing them to cool to room temperature.

Many articles have been written concerning the effect of each variable involved in oxygen cutting and this information may be found in publications on this subject, such as *The Welding Handbook* of the American Welding Society, and pamphlets published by the International Acetylene Association.

Hand Cutting

One factor contributing to greater productivity for a given amount of labor and gases is the segregation of castings according to the size of the risers or gates. In production cleaning operations, changing nozzles to suit material thickness would be time consuming and the operators endeavor, therefore, to cut all sizes of castings with the same nozzle. It is generally found



Fig. 4-Cutting Bench for Medium Castings

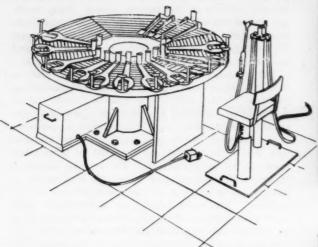


Fig. 5-Rotary Cutting Table for Small Castings

that oversize nozzles are employed so that the operator will be able to cut through any section which is encountered. This results in a waste of oxygen when oversize nozzles are used in cutting small risers and gates. By segregating the castings, each operator can use a nozzle of suitable size for cutting the material directed to his cutting location. Segregation of castings into two size groups has enabled some foundries to effect an oxygen saving of 25 to 30 per cent while processing the same tonnage.

Wherever possible the cleaning floor should be laid out so that there is a planned flow of material from the shake-out machines to the cutting location and on to annealing or machining operations. Likewise, the pouring schedule should be so arranged that a maximum number of identical castings are made at



Fig. 6-Riser Cutting on Cleaning Floor



Fig. 7-Contour-Shaped Riser Intersection



Fig. 8-Hand-Cutting Symmetrical Casting

the same time. The repetitive motions followed in the batch handling of such castings contributes to more rapid operation and reduced cost with increased productivity.

The majority of small and medium size castings, of less than 100 lb in weight, are cleaned by passing them through a shot or grit blasting machine following the initial shake-out operations. The small castings should be spread out on a cutting table to facilitate riser removal. Figure 2 illustrates the extent to which segregation can be practiced when large production quantities are involved. Cutting operations will be speeded up by placing as many similar castings as possible in the same position on the cutting table to provide uniformity of positioning and cutting movements.

In many jobbing foundries several small castings of different design are poured from the same melt. Batches of these castings reach the cutting location where they are deposited in piles as indicated in the background of Fig. 3. The cutting table is loaded with as many castings of similar design as can be conveniently picked up from the pile. In this picture six castings of identical design have been selected from a pile of 20 to 30 different types of castings and



Fig. 9-Cutting Benches Loaded by Helper



Fig. 10-Fatiguing Operating Position

these have been placed on the cutting table in a manner requiring a minimum amount of effort for rapid riser and gate removal. As the operator progresses along the table the risers and scrap pieces are dropped over the far side of the table into scrap buckets while the individual castings are tossed into the skip. These castings are ready for rough grinding, heat treatment, and finishing.

Medium size castings may be loaded on benches in a similar manner as illustrated in Fig. 4. In this case production operations can be expedited by using a helper and an overhead monorail with electric or pneumatic hoist for loading the bench. This enables the skilled cutting operator to be more productive during working hours.

Figure 5 shows a conception of a rotary cutting table which is loaded on the far side by a helper who also removes the severed castings and risers. The cutting operator is provided with a seat. His rate of productivity is high and his fatigue rate low. A version of this arrangement having an annular table or merry-go-round with the operator sitting in a centrally located bucket seat has proved its value in actual production. The operating, or trigger, time with the latter setup has been upwards of 75 per cent of the 8-hr shift.

When the size of casting exceeds the capacity of table equipment, it is desirable to provide a space on the cleaning floor for riser removal. Figure 6 shows a cleaning floor setup for castings requiring overhead crane service because of their weight or bulk. Here a four-man team of clean-up man, set-up man, burner, and inspector keep up a steady flow of material. The castings are handled in rotation so that the space is efficiently used.

One of the castings in Fig. 6 is shown in Fig. 7 as a close-up to illustrate the extent to which the burner can efficiently reduce the amount of subsequent grinding. The curved surface of this ordnance part was the location of the riser. The burner severed the riser from the casting and shaped the surface by four trimming passes.

Figure 8 shows a large gear blank or flywheel having four risers symmetrically located around the rim and with a centrally located feeder. The location of the risers with respect to the rim sections requiring two cutting operations: the first in which the riser is cut off flush with the side surface of the rim; the second, which is made after the upper portion of the riser has been removed, to clear away the riser material along the outer surface of the rim. This cutting time could be materially reduced if the risers were located beyond the outer circumference of the rim. This would permit severing the risers by a single cutting operation through a thinner section of material.

The same cutting table or bed that was shown in the previous illustration may be used for supporting somewhat smaller castings. Figure 9 shows two operators working on adjacent beds which are loaded by a helper. These beds require less crouching or bending on the part of the operator than if the castings were left on the floor. Wherever possible clearance should be provided below or beyond the riser so that the slag can be discharged freely from the cut.

Figure 10 shows the operator bent over with his elbow resting on his knee for support in guiding the blowpipe. He will bend over further or crouch down in order to sever the main riser on this casting. His daily production could be increased if the operating positions were less fatiguing.



Fig. 11-Operating with Complete Control

A typical heavy cutting operation is shown in Fig. 11. The operator is completely at ease, has full control of the blowpipe with his arms steadied on his knees, and has an overall view of the entire reaction zone. The accuracy of the cut in this case closely approaches that of a machine operation.

Another cutting operation through a heavier section is illustrated in Fig. 12. Here the cut is being made along a curved path to reduce subsequent machining. The operator is in a position to see through the kerf and thereby provide proper advancement for complete uniform penetration. He is using a blowpipe with a 180-deg head and a bent nozzle which facilitates positioning.

Extremely large castings having intersections between riser and casting exceeding the capacity of the hand blowpipe may require the assistance of an oxygen lance in order to complete the severing operation. This type of cutting is illustrated in Fig. 13 in which two operators are at work in removing a riser from a pit-poured casting. One operator is maintaining the reaction zone on the near side of the riser with a hand blowpipe while the second operator carries the reaction zone through the material by a forward motion of the oxygen lance. After reaching the far side of the riser the lance is drawn back to the reaction zone on the near side and the operation is repeated until the riser has been severed. The oxygen lance consists of a length of small-diameter, black-iron pipe fed by a large volume of oxygen through a control valve in the lance handle.

Mechanized Cutting

Although considerable effort has been expended in an attempt to mechanize riser-cutting operations, it has been found that manual-cutting operations are more effective in the majority of steel foundries. This is due to the fact that most steel foundries produce castings in job lots for which the faster positioning with the hand blowpipe offsets the advantages of mechanization in small quantity production. There have been several instances, however, in which mechanical riser cutting has been effectively employed for special jobs. These generally had symmetrical or symmetrically located risers and were



Fig. 12-Riser Cutting Along Curved Intersection



Fig. 13-Lancing



Fig. 14-Machine Cutting Test Bars

produced in sufficient quantity to justify preparation of holding jigs and fixtures for automatic or semi-automatic cutting operations. The cutting movement in these cases may be straight line or rotary; or a combination of straight-line and rotary movement; or shape cutting. In general the cost of cutting gases plus labor for these operations is less than the cost of grinding wheels plus labor required to clean up hand-guided cuts. An example typical of straight line cutting operation shown in Fig. 14 which illustrates preliminary preparation of test bars prior to machining. These test bars or coupons were cut to within 1/4 in. of the end of the piece to facilitate breaking by the inspector while keeping the pieces

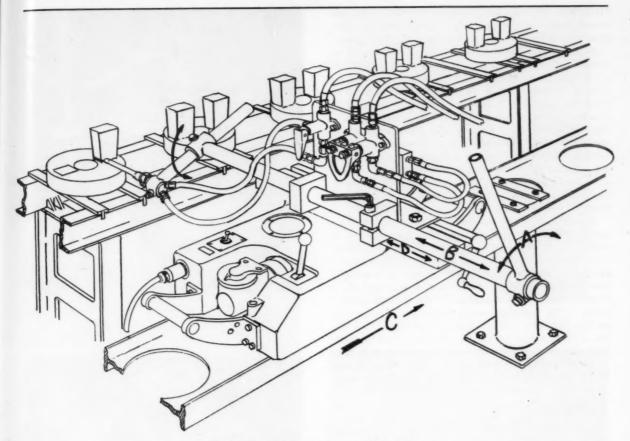


Fig. 15-Mechanized Riser Cutting Setup



Fig. 16-Mechanized Cutting Centrifugal Castings

intact for identification.

In Fig. 15 the sketch illustrates a method which has been used in the removal of risers from castings having the line of cut parallel to the underside of the casting. This permits the castings to be mounted on supporting bars on the table in which position the risers can be severed by a smooth cut close to the

casting surface. The horizontally located nozzle can be rapidly positioned vertically or horizontally by a rotation and transverse sliding movement of the supporting arm. The movement of the arm is locked up by tightening the Allen set screw. The uniform cutting movement is controlled by the speed governor of the straight-line cutting machine. This operation greatly reduced the amount of subsequent grinding.



Fig. 17-Mechanized Cutting Symmetrically Located Risers

However, mechanization proved to be somewhat slower and was dropped, after hand-blowpipe operators began cutting off risers closer to the castings.

A rotary movement is required in the removal of annular risers which are most frequently used in centrifugally cast parts. Figure 16 shows a centrifugal casting mounted on a motorized turn-table. The blowpipe supporting post is positioned so that it can be used on either one of two turn-tables. In the removal of the riser, the cut was started at the top surface and the blowpipe supporting arm lowered until the nozzle was cutting along the desired parting line. It has been estimated that this foundry effected a 50 per cent saving by the elimination of grinding, chipping, and machining operations on a large proportion of their production.

A combination of straight-line movement and rotation shown in Fig. 17 illustrates the removal of symmetrically located risers from a circular casting. The casting is mounted on a rotary table which is turned to position the riser with respect to a straight-line cutting machine. The latter is mounted on a hinged arm which is motorized for vertical adjustment. This combination of rotary table movement and straight-line cutting machine movement provides for a wide range of casting designs. This foundry was able to eliminate subsequent grinding and chipping on castings from which the risers were removed by this mechanized cutting operation. The smoothness of the cut surfaces is indicated in Fig. 18.

Due to the position of the riser or feeding gate and the casting surface, it is sometimes necessary to employ a shape-cutting machine as in the case of removing the feeding gate from pairs of railroad brake control castings. Figure 19 shows the cutting operation progressing on the second of a group of two pairs of castings. It can be noted that the castings are held in a jig and that a formed templet is used for the tracing wheel to follow in controlling the cutting operation. Figure 20 shows the cut surfaces of the castings.

An arrangement for multiple-blowpipe machinecutting in the foundry is illustrated in Fig. 21 which shows the removal of the blind risers from tank tread



Fig. 18-Cut Surfaces From Fig. 17



Fig. 19-Mechanized Shape Cutting for Severing Feeding Gate



Fig. 20-Cut Surfaces From Fig. 19

sections using four blowpipes, simultaneously cutting on a nest of two castings. This piece was an extremely large production item during the war and it was possible to justify the necessary jigging and planning required to effect a minimum amount of subsequent conditioning.

This phase of foundry cutting may be summarized briefly as a process than can be successfully employed in reducing processing costs providing there are enough castings of a general type to run production-line operations. Co-operation between production planning, molding, and cleaning room groups is required to effect maximum economy. One place in which the molding group can help is in placing



Fig. 21-Mechanized Multiple Blowpipe Operation on Tank Tread Casting



Fig. 23-High-Capacity Blowpipe Cutting Riser From Fig. 22



Fig. 24—Surface of Cut Produced by Operation in Fig. 23

square edges at the points where cuts will be started to reduce the preheating time. Also the inclusion of a cored hole through an annular riser reduces starting time and eliminates unnecessary cutting or piercing.

The development of heavy cutting equipment for severing triple-poured armor plate ingots (553,000 lb), ladle buttons, spills, and salamanders has produced a trend toward the gradual replacement of



Fig. 22-Removing Risers From Large Engine Base



Fig. 25-Surface Defects Removed by Gouging

lancing in heavy steel foundries. The risers on many large pit-poured castings are cut while the casting retains some of its original pouring heat so that the cut surface may be covered again with sand top provide for drawing of the surface hardness or thermal stresses that may have developed. Such risers frequently exceed the capacity of hand-cutting equipment so that it is necessary to resort to lancing or cutting plus lancing. This type of operation is uncomfortable, to say the least, in the case of large pit-poured castings due to the fact that the operator



Fig. 26-Gouging out Minor Defects



Fig. 28-Surface Left by Operation in Fig. 27

is practically lying on top of the large mass of warm steel (400 to 500 F). The top surface of the casting is cleared away so that the cut can be made as close to the casting surface as possible, and thereby minimize the height of the remaining pad. Pit-poured castings having risers up to 43 by 72-in. cross-section have been severed by a high-capacity machine-borne oxygen-cutting blowpipe and nozzle. In removing the 56 by 48-in. head from a large casting this blowpipe required only 26 min operating time as compared to 155 hr of drilling. The size of this equipment made it rather difficult to position the nozzle close to the casting surface on large area castings. This condition has now been remedied by the use of a horizontal nozzle with a 90-deg adaptor so that the blowpipe may be located in a vertical plane and supported by a counter-balanced arm mounted on a cutting machine.

Figures 22, 23, and 24 show risers being removed from a large engine-base casting having twelve risers,



Fig. 27-Pad Reduction by Scarfing



Fig. 29-Powder-Cutting 18-8 Stainless Steel Casting

each having a minimum dimension of 20 in. The proximity of the nozzle to the work surface is illustrated in Fig. 23 and the cut surface in Fig. 24. These cuts were made close enough to the casting surface and were sufficiently smooth so that no subsequent pad reduction was required prior to machining of the casting.

Gouging

Another phase of oxygen cutting is the oxygengouging operation which is used in steel foundries for the removal of small defects preparatory to repair by welding. This operation is faster than chipping and is superior to the latter from the standpoint of



Fig. 30-Powder-Cutting and Washing



Fig. 31-Mechanized Powder Cutting Stainless Steel

complete removal of the defect. Any defective material is easily identified in the oxygen reaction zone whereas chipping has a tendency to drag the casting material and seal over cracks or small defects. A casting which has been inspected and gouged preparatory to welding is shown in Fig. 25. The gouging reaction is created by a large-bore nozzle fed by a metering orifice which produces a low velocity oxygen stream. The latter is easily controlled by the operator

in taking out minor defects as illustrated in Fig. 26 or in cleaning out cracks that may run several inches deep. The gouging nozzle can effectively remove defects in confined spaces that are too small for chipping operations as for example, inside valve bodies. Chills, nails, and other projecting sections can be easily removed in cleaning castings by the oxygengouging process.

Pad Reduction

Removal of riser pads or stubs is another application for an oxygen process. The nozzle used for this work has a large oxygen orifice with a partial restriction so that a medium-velocity oxygen stream is discharged from the nozzle. A wide reaction zone is created on the surface of the material and rapidly carried across the pad as shown in Fig. 27. Adjacent passes are made until the height of the pad has been reduced to the level of the casting. In the illustration the operator is working on the lower pad. Figure 28 shows the surface left by the pad scarfing operations. Slag which collected on the far side of the pad is easily knocked off after cooling as indicated. Size of the original pad can be judged from the upper pad which is still in the as-cut condition.

Powder-Cutting

A relatively new phase of oxygen-cutting in foundries is that of powder-cutting which employs an additive of essentially iron powder with the oxygen and preheat gases of the cutting blowpipe for cleaning-room operations on oxidation-resistant ferrous metals. Such metals include cast iron, chrome iron, stainless steel, and high-temperature alloys. The powder is also effective in overcoming sand incrustations and layers or inclusions of slag.

This iron powder is picked up by air as the conveying medium in a dispenser unit from which it is carried to the cutting blowpipe through a suitable length of hose. The iron powder, as an air-borne mixture, is discharged through port openings directed towards the central oxygen stream. This powder passes through the preheat flames where it is raised to ignition temperature so that combustion starts upon contact with the rapidly moving oxygen stream. The combination of preheat flames, burning powder,



Fig. 32-Powder-Cut Surface From Fig. 31

and oxygen attacks the oxidation-resistant ferrous materials by heat, fluxing, and oxidation as rapidly and as readily as the oxygen-cutting of medium-carbon steels.

Various methods of introducing the powder to the reaction zone are provided by different powder-cutting blowpipes, attachments, and nozzles. Each method of introduction appears to have superior characteristics for certain applications. The powder-cutting blowpipe is best suited for manual riser cutting. The cutting attachment can be used successfully for mechanized plate-cutting operations. The powder-cutting blowpipe with gouging nozzle is the combination best suited for defect and sand incrustation removal.

Manual riser cutting on 18-8 stainless steel is shown in Fig. 29, which illustrates the cleaning of rotary pump part. Only finish grinding is required on these parts and the powder-cutting operation provides an estimated saving of 30 per cent in time alone over the previous methods.

A similar powder-cutting operation on another type of casting is shown in Fig. 30. By washing certain parts of the casting surface, the operator is able to clean up the casting so that it requires only a finish grind. The reduction in operating time alone amounted to 300 per cent over that of the carbonarc method.

Mechanized riser cutting on centrifugally cast stainless-steel aircraft engine supporting rings is shown in the next three illustrations. The operation is performed on a repetitive production basis and accurate chucking is provided for location of the rings on the turn table. Figure 31 shows the cut progressing on a ring as it is rotated below the cutting blowpipe. The casting with the riser removed is shown in Fig. 32. A hand scraping operation is all that is required to clean up the casting. After cleaning, the condition



Fig. 33—Powder-Cut Surfaces After Cleaning by Scraper



Fig. 34-Powder-Washing to Remove Burnt Sand in Fillet Sections

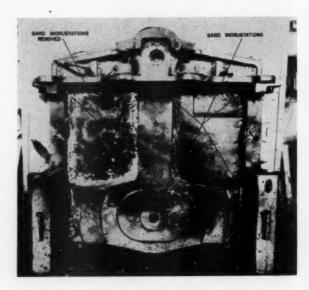


Fig. 35-Surface of Casting Before and After Powder-Washing

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of the casting is as shown in Fig. 33. It is possible to powder-cut these risers in approximately 15 min for 80 linear inches of cutting through 1½ in. of 18-8 stainless steel. The operation uses less than 100 cu ft of oxygen, 13 cu ft of acetylene, and 2½ lb of powder per casting. Only sandblasting and machining are required before the parts are ready for use. Previously risers were removed by boring which required 6 to 8 hr and tied up expensive machinery. This operation is also used on other sizes of centrifugally cast castings.

Mechanized powder-cutting with the C-45 blowpipe has been used to cut large castings of 18-8 stainless steel with solid risers up to 48-in, diameter.

Powder-Washing

Powder-washing of sand-incrusted castings has recently made possible considerable savings in several foundries. This process uses the regular powdercutting blowpipe with a powder-gouging nozzle. In the latter the powder is aspirated into the cuttingoxygen passage at the rear end of the nozzle so that the powder is dispersed throughout the oxygen stream as it leaves the nozzle. For powder-washing, a high preheat flow, normal powder flow, and oxygen pressure less than 25 psi are required. The oxygen valve is barely cracked during the washing operation. In Fig. 34 and 35 sand incrustations along the inside fillet are being removed. The major advantage of the process is a saving in time which amounts to a reduction from hours to an equivalent number of minutes in addition to the elimination of chipping hammer noise. In one operation the removal of a mass of sand and steel from a casting had previously taken 12 to 14 hr by pneumatic hammer. The removal of an equivalent mass required only 13 min by powder-washing. In another instance a shift or lump of steel with sand inclusions (10 by 10 by 2 in.) was removed in 25 min of washing time as compared to between 15 and 20 hr of chipping time.

Powder-cutting is also extremely useful in reducing heavy scrap, open hearth cinder thimbles, ladle buttons, and skulls. Its effectiveness may be gauged by the fact that laminated layers of slag and iron up to 4 ft thick can be cut with mechanized equipment. Cast iron sections up to 56 in. thick have been powdercut as well as steel buttons up to 72 in. thick. External slag layers less than 2 in. thick are readily penetrated by the standard iron powder and cutting attachment for heavy cutting blowpipe. Heavier slag layers have been severed by adding other materials

DISCUSSION

to the iron powder.

Chairman: J. THOMSON, Continental Foundry & Machine Co., East Chicago, Ind.

Co-Chairman: R. J. Wolf, Stone & Webster Engineering Corp., Boston.

WILLIAM BURROWS (Written Discussion): 1 We are in accord with Mr. Babcock's views on economies possible through close supervision of burning operations and the segregation of large and small castings.

Considerable savings in oxygen cost can be obtained in keeping a close watch of tip sizes and gage pressures used in cutting operations. Attention to fatigue positions of the operator pays dividends in closer burning and subsequent reduction in cost of future operations.

We are not in accord with the writer's views on sandblasting or rotoblasting small castings before burning, this can be quite expensive, you can double your amount of castings per load after heads and gates are removed.

If your sand is in shape you can clean castings for the burner much more economically by scrubbing the area of cut with a wire brush similar to those used to clean weld slag.

We agree with the writer's views on the economy of using gouge tips for preparing defects for welding.

In the present day when talk of thousands, millions and billions of our cheapened dollar is quite common, we are not as nickel and dime conscious as we might be. In the present competitive market it behooves us all to pay closer attention to these small leaks. A tighter ship has a much better chance of reaching port on time than one bedeviled with numerous small leaks.

¹ Supt., Atlantic Steel Castings Co., Chester, Pa.

In reviewing Mr. Babcock's paper on oxygen cutting processes in the cleaning room, the writer leaves the impression that mechanized cutting can only be employed for special production jobs, that it is necessary to have special jigs and fixtures to position the jobs.

We are attempting to machine burn our larger castings ranging from 300 to 15,000 lb. We are not yet able to make all our cuts with this method, but our percentage is rising each week. We also realize some designs are not adaptable to this method.

We operate a jobbing shop so we seldom have more than one of any design in sequence. You can readily see it would be impractical to position the jobs to a stationary machine.

We are trying to lick this problem by developing a rigging to position the machine to the job. Our present rigging, which is still in the development stage, allows us to machine burn any size head, in any position, in a range of 1 ft to 6 ft from floor level.





The rigging is fairly simple. We have secured the track and machine to a 6-ft piece of 6-in. channel suspended by a small chain hoist from a swinging jib crane. To burn heads we simply push the machine in position, raise or lower to the desired height, adjust the torch to the proper angle, screw two adjustable supports to the floor to prevent any sway or vibration, start the cut and the machine does the rest. The only move necessary by the overhead crane is to bring the job to the floor and remove the casting and heads after burning.

We have not progressed far enough to have presentable cost figures. We feel we can show considerable savings in eliminating the grinding operation and the extra handling plus the welding of undercuts.

The limitations of the burning machine should be considered in the planning of heads and gates. When it is necessary to place heads in from the edge of casting, or use a round head, provisions should be made to extend the base of the head to the edge of casting. This can be done by using 1-in. burn pads either cut or placed in the cope. This enables the operator to cut flush with the casting surface.

This problem can be solved more easily by having some member of the planning committee familiar with the operation of the machine.

A gratifying condition has developed since we have been using the machine, the burners attempt to copy the machine cut on what hand burning they do.

The accompanying illustration shows the rigging and its application.

In commenting on the powder torch for the removal of fused sand; the tool is one of the most economical in the cleaning room. The fused sand problem is one of the worst the foreman has to contend with. It is hard and expensive work for the chipper. The powder method is easy, clean and economical. When we first received this tool we were processing a job with a difficult pocket to clean. The operating time was reduced from

11 chipping man-hours to 40 minutes burning time.

The foundry is continuously trying to eliminate burned in sand. However, occasionally a casting arrives in the cleaning room with fused sand. The foreman is able to use this tool and perform the necessary operations with very little physical effort and at considerably less cost.

T. S. QUINN, Jr.: 2 Do you have comparative costs on the use of oxygen and machine cutting as against hand cutting with a torch?

MR. BABCOCK: There are too many variables involved to give a clear cut answer. However, I believe you will find that if the setup and the time required to work it out can be justified, mechanization will give more economical cutting operation. The cost of mechanization will have to be balanced out with the amount of production that you have, but your mechanized operation will require less gas because it enables you to use the proper nozzle size for the section that is being cut, whereas in manual operations, the operator generally uses an oversize nozzle which is large enough to take the maximum section which he expects to encounter in his cleaning operation. He may be using a nozzle for cutting 3-in. thick material that is capable of severing 12-in. thick steel, so in that case, he is wasting oxygen but maintaining his production at a high level.

We would have to consider these and many other factors to determine whether it is more economical to machine cut or to sand cut a particular casting.

Co-CHAIRMAN WOLF: The biggest advantage of machine cutting is the fact that you have to do less final finishing to make the casting ready for shipment or use. I had a great deal of experience with this problem during the war. In one case I was able to eliminate approximately 56 hr on a large planer by substituting machine cutting which required 4 to 6 hr. This job required 7 to 8 hr for hand cutting.

² Supt., Lebanon Steel Foundry, Lebanon, Pa.

REFRACTORY PRACTICE IN ACID ELECTRIC STEEL MELTING

By

R. H. Jacoby* and M. Petty**

INTENDED TO SERVE as a guide in establishing or improving refractory practices in the foundry melting acid electric steel for medium-small steel castings, no attempt has been made in the present paper to include ceramic theories. Rather, the purpose is to present a practical outline of established practices in electric furnace shell lining and roof construction, ladle lining, and refractory repair and storage.

Furnace operations in the authors' foundry are concerned with a 13/4-ton direct-arc electric furnace (Fig. 1). A top-charge, swinging-roof unit, the furnace shell is lined with 13-in. silica brick. The bottom is of a molding sand mixture rammed in to a depth of 12 in.

The molding sand mixture is rammed to a predetermined depth over the bottom and is shaped from the center out, sloping up to the bottom of the charging door and spout opening. A ledge is created at this height completely around the furnace, at right angles to the side wall, extending in 13 in. from the shell. This is done in order to establish a sound foundation for the silica brick side walls.

Side-wall construction is started at the door and spout openings. These particular brick are usually wedge-shaped in order to incline toward the center of the furnace. In continuing in a radial fashion with the side-wall brick, the bricks are tilted downward about ½ to ¾ in. from the level. This is the allowance made for the inner brick face expansion during the operation of the furnace, which will tend to level the bricks in relation to the furnace.

The bricks in the next course are staggered, as is standard practice in any bricklaying operation. The door jambs are started on each side of the charging door, special shapes being used for this purpose. Subsequent courses of brick are laid following the same procedure. The tapping hole size is governed by personal preference. In this particular case, a rectangular opening 8 in. wide by 12 in. high is allowed (Fig. 2).

Fig. 3—The charging door must be carefully constructed. Skew bricks are placed on top of the door jambs, and wedgeshaped bricks on the arch.





Fig. 2—The method of constructing the tap hole is shown in this view. In this furnace the tap hole dimensions are 8 in. width and 12 in. height.

At the top of the door jamb the arch must be set. The setting of the arch is important and should be carefully done. General procedure involves the placing of a wooden arch form across the edges of the two door jambs. A skew brick is laid on top of each jamb, and the wedge-shaped arch brick are laid in a radial manner toward the top of the arch. At the top, the selection of brick size is made to give a secure fit of all the shapes in order to insure good unit strength of the arch (Fig. 3).

The curved top of the arch is levelled off with addition of a mulled gannister mix of 30 per cent fire gan (3/8 in. and smaller rock gannister), 30 per cent fire clay, and 30 per cent coarse silica sand. This provides a flat, level base for the roof ring over the arch and eliminates the possibility of any burning of refractory at this point during furnace operation. Laying of the courses of side-wall brick is continued to a point parallel with the top of the furnace shell side walls. The last courses are worked over to the conjunction



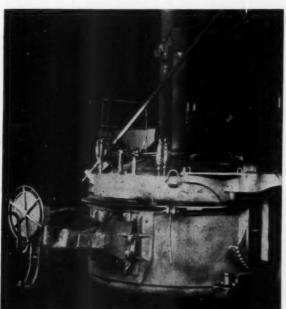


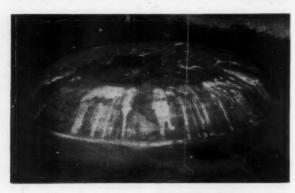
Fig. 1-The melting unit is a 13/4-ton direct-arc electric furnace of the top-charge, swinging-roof type.



♠ Fig. 5—Cardboard expansion strips are inserted between every fourth brick on each course of the roof brick. Fig. 6—Special shapes are used to ▶ construct openings around electrodes.

Fig. 4-A wooden form is used for laying up the brick in the roof ring.





with the arch in the charging door. The whole assembly is finshed off with a layer of gannister mix.

The furnace bottom is then carefully worked to a bowl shaped curvature, the depth of which is controlled by the use of a wooden gage running between the charging door sill and the spout. A measurement of 14 in. is held from the center of this gage down to the rammed furnace bottom. The breast, the area in front of the spout, is shaped to insure the even and complete emptying of the furnace when tilted for tapping. Finally, the spout lip extending off the front of the furnace is rammed in with a gannister mix, contoured to a trough shape and washed with a slurry.

Upon completion of the "ram-in" on a new furnace bottom a wash of glutrin water is applied to the surface, followed by a 30-min drying period by means of arcing against crossed scrap electrodes on the bottom.

Extra roofs for the top-charge, swinging-roof furnace are made up and kept ready for immediate installation in the event of failure of the roof in operation. A wooden form is used for laying up the brick in the roof ring (Fig. 4). The roof ring consists of a channel shaped concentric section of steel used as a retainer and holding mechanism for the roof.

Before proceeding to lay the brick in the roof ring the concentricity of the ring should be checked and, if water cooled, tested for leaks. This is necessary because a ring often becomes warped in service; when the warpage becomes too severe the ring must be straightened, and leaks often result.

The bricks in the first course of the roof ring are known as skew bricks, and they are specially shaped to conform to the particular design of water chamber in the roof ring. In laying the roof brick cardboard expansion strips are inserted between every fourth brick on each course (Fig. 5).

Roof bricks are made in standard sizes and special shapes. In this foundry special shapes are used—fewer bricks are required, less skilled labor can be used, and the roof can be laid in a shorter time (Fig. 6). These factors offset the higher initial cost of the brick.

Roof brick shapes are carefully set up to the area of the electrode openings, where three plugs are inserted into the wooden form. These plugs represent the allowance for electrode openings in the roof. As the shapes forming the electrode openings are special, it is a simple matter to insert them in their proper places across the center section of the roof. The key brick is round and in the immediate center of the whole assembly. It should only be necessary at this point of the assembly to slide this brick into place.

Start New Roof on New Lining

Storage of a complete roof should be effected at least 3 to 4 ft off the ground. In the assembly of a roof on the furnace an attempt is made to start a new roof on a new lining in order to avoid the overhang of roof brick that is involved when a new roof is put on an old lining. Excessive spalling can generally be expected when it becomes necessary to install a new roof on an old lining.

Operating methods will affect the life of the furnace refractory. After each heat is tapped an examination is made of the furnace bottom. A silica sand and glutrin mixture of the consistency of core sand is placed where any holes or erosion appear. Particular scrutiny should be given to areas around the slag line on the furnace and the areas in front of the charging door and the tapping hole. The charge is placed care-

fully in the furnace with the heavier material on the bottom. After tapping the furnace the tapping spout trough outside the furnace is chipped off and repaired,

using a molding sand mix.

During the life of a furnace lining it generally becomes necessary to replace the charging door arch and door jambs. These repairs can be made on the cold furnace before starting in the morning. The old bricks are chipped out and the new bricks set in place. This work, along with the replacement of two or three top courses of side-wall brick, is common practice during the life cycle of a furnace lining.

During a shut-down period the condition of the side walls, bottom door and spout areas must be closely observed. This is done to avoid operating the furnace to a point where metal would burn through the refractory and shell, creating a maintenance problem as well as an expensive shut-down period. The furnace bottom is not replaced during a furnace reline. Instead, any metal is chipped out from around the furnace bottom. It should not be necessary to ram more sand on the bottom during a reline. Good furnace practice between heats calls for the proper sanding in of the furnace in order to retain a reasonably smooth, contoured, consistently level furnace bottom. During operations the furnace roof is kept well cleaned of dust and dirt.

During a shut-down period a charge is placed in the furnace and the roof swung back onto the furnace. This is to insure the minimum of thermal shock to the roof during cooling. Generally, the roof life is shortest in the center section. This is probably due to a combination of erosion from the arc and heat reflection from the slag-metal surface. It is possible in good practice to replace the center brick in order to lengthen the time of service of the roof.

Lining the Ladle

The bottom-pour ladle is used exclusively in the authors' foundry. This ladle is lined with a rammed-in mulled gannister mixture consisting of 30 per cent rock gannister, 30 per cent fire clay, and 30 per cent coarse silica sand. The mixture is rammed with air hammers around a metal-covered wooden form which is then drawn from the ladle, leaving a well contoured lining (Fig. 7).

It is important that the gannister mixture be added around the form in small quantities and rammed down hard before adding more. This is to insure the maxi-

mum density of the rammed-up material.

The bottom is rammed in next, tying in carefully with the side-wall material. A large clear spot having been left in the nozzle area, the nozzle is inserted and the gannister rammed hard around the nozzle and the bottom shaped and smoothed over into the nozzle well. The completed ladle shell is then brushed over with a clay slurry, set aside and allowed to air dry for at least 48 hr. After this period the ladle is dried on low fire, about 1000 F, for 8 hr, then turned up to a cherry red, about 1600 F, for at least 15 hr before use (Fig. 8).

Fig. 8—Ladles are air-dried for a period of 48 hr—on a low fire at 1000 F for 8 hr—and at a cherry red (1600 F) for 15 hr before use.

Fig. 7—The bottom-pour ladles are lined with a gannister mixture. Air hammers are used for ramming up the gannister mixture around the metal-covered wooden form.





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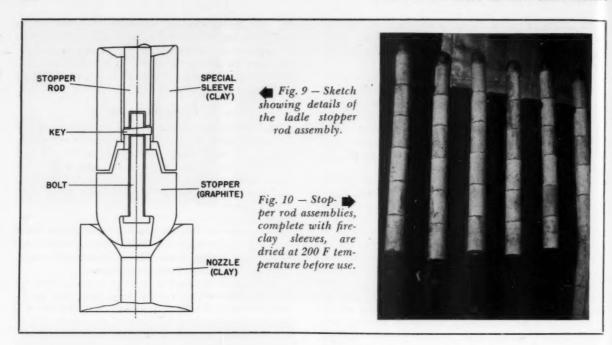
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The stopper rod assembly consists of fire-clay sleeves the length of the rod, the black head, stopper pin and wedge (Fig. 9). In assembly the stopper pin is assembled in the black head. The black head assembly is inserted in the stopper rod and the wedge driven in place, and subsequently covered by the fire-clay sleeves, all joints being cemented in and to the rod with high-temperature cement (Fig. 10). The opening in the black head is filled with a special cement supplied by the black head manufacturers. The entire assembly is allowed to dry at a low temperature—about 200 F.

In setting up the ladle for a heat the ladle is brought

Fig. 11 – A coil spring located between the top nut and the last sleeve on the stopper rod assembly allows for expansion of the clay sleeves.

off the fire and turned upright. The stopper rod assembly is dropped in position from the crane and bolted on. A coil spring located between the top nut and the last sleeve allows for expansion of the clay sleeves (Fig. 11). The assembly should be adjusted to produce a wiping motion across the face of the nozzle in closing. This clears the nozzle and insures a better seat in the ladle bottom.

After installation the stopper is closed. A handful of fine silica sand is thrown around the stopper and nozzle junction. A shovel placed under the nozzle and beneath the ladle bottom is subsequently examined for any leakage of the fine sand thrown in the ladle. It is important that the sand be blown out of the ladle before tapping.

After each heat the nozzle is knocked out of the ladle. The stopper rod assembly having been pulled, the sleeves are broken loose and the rod and black head pin returned for reassembly. After the ladle cools sufficiently the slag and metal, if any, around the top of the ladle are chipped down. The bottom is cleaned and the area where the nozzle is to be reinserted chipped out clean. A new nozzle is positioned and a sand-gannister mixture used for setting in place. Gleaned of refuse, the ladle is dried at a red heat and returned for another use.

Refractory Storage

Due to the relatively high cost of furnace refractory, storage facilities are such as to preclude the possibility of abuse in handling, resulting in excessive breakage. A perpetual inventory is carried for all melting department refractories. The furnace shell, roof, and ladle lining condition, as well as the number of heats, are recorded on each heat log.

Since any discussion of refractory life and service must necessarily concern itself with the service and operating conditions to which the refractories are subjected, comparisons become relative even if good practice prevails. Furnace linings and roofs in the shops melting for small, intricate shank castings would have problems not encountered in the melting of metal for heavy carbon-steel castings in a similar furnace, where tapping temperatures would be lower. Likewise, continuous melting of high-alloy steel in a furnace shell would create an entirely different picture as compared with the production of straight, mediumcarbon steel in the same shell.

The melting unit in the authors' foundry operates on a 16-hr per day cycle. Over 60 per cent of the tonnage is alloy steel of the chrome-moly, chrome-nickel grade, including the stainless classifications. Tapping temperatures are not high, as no shank work is involved. The roof life is about 170 heats per roof. Furnace lining life is about 200 heats. The furnace bottom has been replaced only once in the past 8 years. Average ladle life is 65 to 70 heats per ladle. The ladles handle an average heat of 4 to 41/2 tons.

DISCUSSION

Chairman: R. H. STONE, Vesuvius Crucible Co., Swissvale, Pa. Co-Chairman: R. A. WITSCHEY, A. P. Green Fire Brick Co., Chicago.

E. A. Swenson: 1 How much sand do you put into your furnace between heats?

MR. JACOBY: We add no more than 75 to 100 lb of sand to our furnace between heats. Very often it is not necessary to put in any sand at all.

Our furnace refractory practice improved in the past few years and a change in melting practice itself has probably tended to improve the conditions a great deal. We use oxygen injection and with the adoption of oxygenation of the heats, our furnace refractory life has increased materially, particularly with respect to the bottom. Sanding in of the furnace is at a mini-

MR. SWENSON: What is the life of the walls and the roof of the furnace?

Mr. JACOBY: We are getting about 200 heats.

MR. Swenson: Do you think the weakest part of the roof is around the electrodes?

Mr. JACOBY: That has been our experience.

Mr. Swenson: Are those special shapes you are using around the electrodes?

MR. JACOBY: Yes.

J. H. RICKEY: 2 In connection with Mr. Swenson's remarks about the wear around the electrodes, we have found that the use of an air-setting plastic improves the life at that point, particularly where you have intermittent operation, and it will stand that spalling condition much better than the silica shapes.

MR. JACOBY: We put in supersilica shapes which improved things materially.

MEMBER: Have you had experience with segment linings; by that I mean with a roof, consisting of 12 to 16 segments outside of your electrode center area, either with plastic material or ramming material or cast fire brick?

Mr. JACOBY: I would assume that you have an expansion problem there with silica. That is done with other types of refractories, the sillimanite refractories, where they use monolith which becomes a unit link in service.

CHAIRMAN STONE: That would not be very good practice because it is an advantage in having small pieces because you know where the cracks are coming. The cracks are already there. If you have the big blocks, I would imagine that you would have a lot of cracking trouble. That is just a theoretical answer.

Co-CHAIRMAN WITSCHEY: There is another point to be considered. In making large special shapes it is difficult to manufacture refractory body which possesses physical characteristics comparable to smaller power-press or machine-made shape.

L. P. WILSON: 8 We have an Electromelt furnace, size V in which we are using a one-piece top. It is not silica. I was apprehensive when we started up the furnace as to where cracks would develop. The refractory held up very nicely. We are operating well above 3300 F. We have gone up to where sillimanite dripped. That particular top is about 40 in. in diameter and is a one-piece job. The cracks eventually came, but they did not come in a spot where the top would fall

I imagine it is made against the form, rammed in and pre-

CO-CHAIRMAN WITSCHEY: It is possible to make large refractory shapes by different methods, i.e., the ordinary wood mold or handmade methods, and then there is a special air-ramming method which some of these electric furnace producers have to employ to make the form at all.

Those air-rammed shapes are better than the handmade shapes, but in general they still are not comparable to power

press shapes.

MR. WILSON: Some furnace manufacturers are installing refractories by means of the refractory blow gun which results in closer compacting. This gives you a greater density material when it is gunned, and it has less moisture in it. Moisture causes trouble in any furnace top or rammed side wall. We are using complete rammed side walls in the furnace, then we dry the refractory by means of infra-red ray heat and finally fire during the first melt.

MEMBER: How thick is your furnace top?

MR. WILSON: It is 6 in. thick.

MEMBER: Is sillimanite a heat reflector or a heat absorber? Mr. Wilson: I suppose it is a heat reflector. There is very

little heat that comes out of that top.

L. N. PANGBORN: 4 You stated that you get 200 to 250 heats from your side wall. How many heats do you get in a 16-hr

MR. JACOBY: We get 7 heats per day. The furnace is down

on week-ends, Friday to Monday.

A. H. Thomson: ⁵ Has Mr. Jacoby tried fireclay refractories in his furnace roof and side walls. In view of your rather intermittent operation, shutting down over the week-ends, have you considered the possibility of better service with other refractories in your side walls?

MR. JACOBY: No, we did not try fireclay for this application. We have considered sillimanite and monolithic. However, when you consider the cost and refractory life expectancy, it just does not pay. We went over this three times in our operating history and gotten estimates on roof costs and roof linings.

Mr. Thomson: There is a great difference in cost but there is improved life in the walls and in the roof. The question resolves itself into whether it is more profitable to pay much for refractories and avoid shut downs or less and have shut downs.

C. E. Bales: 6 If you are operating intermittently, the airsetting plastic around the electrodes would help out considerably. When times get dull and you want only one or two heats per week, even a rammed side wall can be satisfactory for you instead of silica brick. Silica brick spalls and you cannot do anything about it, but a lining made of siliceous ramming refractory, rammed around a form, will give considerably longer life than regular silica brick.

V. R. HORNING: 7 We have a rotary melting furnace fired with pulverized coal. We have a problem of keeping burner blocks in this furnace. We tried silica and special refractories but none are satisfactory. What could we use to increase the life of the burner block? The size of the opening is 7 in. We are firing with a Whiting pulverizer. We are either loosening the burner block out through abrasion or we are getting a thermal shock due to the air coming into the block, and the radiation we get and penetration into the block after we shut down.

CO-CHAIRMAN WITSCHEY: Many of you are familiar with the Brackelsberg furnace in malleable iron. It was used for many years in the past, but very few are still in use. Those of you who had anything to do with refractory problems on that furnace know the silica brick was most satisfactory.

This furnace that Mr. Horning describes is an experimental furnace. It is a cylindrical furnace, conical at each end. Burners come in at an angle from the rear cone and the furnace tips and the front cone end is used for pouring the metal. The cone at the other end is used for charging the metal through a long

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⁵ District Sales Mgr., Canadian Refractories, Ltd., Montreal, Canada.

⁶ Pres., The Ironton Fire Brick Co., Ironton, Ohio.

⁷ Mgr., Foundry Div., Columbiana Pump Co., Columbiana. Ohio.

tube and also for containing the burners.

The metal then is in a furnace which rotates and is charged at one end and tapped at the other end. It is charged continuously by batches and tapped continuously by batches.

Mr. Horning: We tap about 1000 lb every 10 min.

CO-CHAIRMAN WITSCHEY: It is a continuous intermittent furnace.

Mr. THOMSON: Has Mr. Horning tried 90 per cent alumina rammed blocks?

MR. HORNING: We have used it in there, but our chief trouble was that it would not tap. It fell out.

MR. THOMSON: Have you tried the burned shape?

Mr. Horning: No we did not try that. The only one was silica.

Mr. Thomson: I would recommend the 90 per cent Al₂O₃ burned block. In our kilns we have severe burner block trouble and we tried an 80 per cent chrome and 20 per cent magnesia block which is satisfactory.

MR. HORNING: The trouble is partly brought about by the size of the burner block; it is 7 in. in diameter. We seem to have trouble holding that size together. It tends to spall considerably.

MR. THOMSON: We are making blocks now in that type of

material and they are enormous pieces.

CHAIRMAN STONE: There is one point Mr. Swenson brought out previously about the standard shape, and Mr. Jacoby said a special shape is required. Isn't your furnace a standard furnace I should think whatever you need would become a standard shape, unless they made a special furnace for you. Would not the shape you are using become a standard shape for other users.

MR. JACOBY: That is right. We have had one roof straightened I think the past 2 or 3 years. So really roof frame warpage is not too serious a problem, once you have paid for your mold charges, and if there are other shops in the district that are using the same style furnace you have, you prorate mold costs and also carload lots and the cost becomes lower as a result.

MR. HORNING: Does any refractory manufacturer make an end wedge and side arch, a combination of brick of that type?

CO-CHAIRMAN WITSCHEY: Even those can be lined with standard series of shapes, but almost always there is a certain amount of cutting necessary. However, it can be held to a minimum. There are ways of lining cones in standard shapes.

Mr. Horning: Would lining this particular furnace with a basic lining bring about more satisfactory results?

Mr. Thomson: This furnace melts iron under an acid slag therefore basic brick cannot be used.

FURNACE ATMOSPHERE FOR MALLEABLE ANNEALING

By W. D. McMillan*

ABSTRACT

This paper discusses the composition and chemical reactions of atmospheres used in malleabilizing furnaces, the causes of ferritic and pearlitic rims, the compositions effective in retarding decarburization, practice and precautions necessary with inflammable atmospheres.

Results obtained using a high nitrogen atmosphere are given. "Carbon pressure" and the advantages of a low combustible atmosphere are discussed.

MALLEABILIZING OF WHITE CAST IRON involves temperatures at which such castings will scale and decarburize. It has been and is the practice in the industry to establish conditions which inhibit or control these reactions. The practice of annealing as originally developed contemplated packing the castings in a chemically active material. Now it is recognized that the use of packing is not essential in the process.1

Control of the surface condition may be accomplished by mechanical means. Protection against action of furnace gases may be provided by using closed pots sealed with fireclay. The same principle of protection is used in the muffle type furnace in which no pots are used; the entire furnace muffle being sealed to exclude air and products of combustion.

When hard iron castings are heated in closed chambers, an atmosphere relatively rich in carbon monoxide is developed. Some carbon dioxide may be present depending upon availability of oxygen. The amount or per cent of CO developed varies with temperature, time, carbon content of the iron and volume of a chamber, which is sealed to prevent the admission of air. Presence of moisture and/or hydrocarbons plus admission of air will affect the amount and relative per cent of CO and CO2. Practically or commercially, it is necessary to deal with a chamber into which admission of some air is almost unavoidable.

With batch type and continuous furnaces where castings are placed in open containers and the furnace design is such that the products of combustion do not enter the furnace chamber, satisfactory control of oxidation may be obtained by the use of a suitable atmosphere which may be either generated in the furnace or externally prepared and introduced into the furnace.

Four types of atmospheres which are in the more or less common usage are shown in Table 1.2

TABLE 1 1 3 4 Type* 201 102 102 CO, per cent 9 10.0 10.0 10-50 CO₂, per cent None 10-0 H₂, per cent 2 15.0 15.0 N₂, per cent 96 70 70 Balance Dew Point, °F

Typed according to American Gas Association

36

70

36

No. 1, 2 and 3 are externally prepared atmospheres. No. 1, Type 201 prepared nitrogen base with lean mixture, CO, removed by chemical absorption and water vapor by refrigeration. (93% Aeration)

No. 2, Type 102 exothermic base. Combustion of gas-air mixture; water vapor removed by refrigeration.

No. 3, Type 102 same as No. 2 except that water vapor is removed by cooling with tap water.

No. 4 is representative of the range of composition that prevails in batch type sealed furnaces. There is a considerable range in composition throughout the duration of the heat. The CO will be highest toward the end of the high temperature holding period. Water vapor and hydrogen may be present in varying quantities but as these are mainly due to the original moisture in the air or surface moisture, and possibly occluded hydrogen on the castings, the percentages and effect are of minor importance.

Castings are subject to certain reversible chemical reactions which may be oxidizing or decarburizing. Some of these reactions are represented by the following equations:3

TABLE 2 1 Fe + H₂O → FeO + H₂ $\begin{array}{c} \text{Fe} + \text{CO}_2 \Longrightarrow \text{FeO} + \text{CO} \\ \text{CO} + \text{H}_2 \text{O} \Longrightarrow \text{CO}_2 + \text{H}_3 \end{array}$ Fe₃C + CO₃ → 3 Fe + 2CO CO₂ + C→CO 2H₂ + Fe₃C₂3Fe + CH₄ $CH_{4\rightleftharpoons}C + 2H_{3}$ $Fe_{2}O_{4} + 4Fe_{2}C\rightleftharpoons 15Fe + 4CO$

The reactions represented by the equations shown in Table 2 may be oxidizing or decarburizing either di-

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rectly or indirectly. With the exception of Eq. 8 which is the "mill scale" reaction, the equations pertain to constituents in the furnace atmosphere. Being reversible, the direction to which the reaction will proceed is influenced by the relative percentages of the interacting gases and by the operating temperature.

Because 1700 F is fairly representative of the maximum temperature used for first stage graphitization and because of a conventionally long holding period at 1350 F equilibria at these temperatures are discussed.

Eq. 1—Water Vapor and Iron—This is a direct ratio of H₂O to H₂. At normal operating temperatures, this ratio should be less than 4 to 10 in order to inhibit oxidation. Hydrogen must be present in excess of the amount of water vapor present. As the dew point is lowered, the hydrogen content may also be lowered. Conversely, with a low hydrogen, the moisture should be correspondingly low.

Eq. 2—Carbon Dioxide and Iron (Fig. 1)—The equilibrium for the Fe-FeO-CO-CO₂ system is a direct ratio of CO₂ to CO. At 1700 F the ratio should be less than 4 to 10; at 1350 F a ratio of 6 to 10 should be sufficient to inhibit oxidation of iron.

Eq. 3—Carbon Dioxide and Water Vapor (Fig. 2)—In this system, CO_2 — H_2 —CO— H_2O , which are the four principal variables, the equilibrium constant K is $CO_2 \times H_2$ and the curve is plotted against tempera- $\overline{CO \times H_2O}$

ture with a logarithmic ordinate.

In the first two reactions discussed the oxidizing fac-

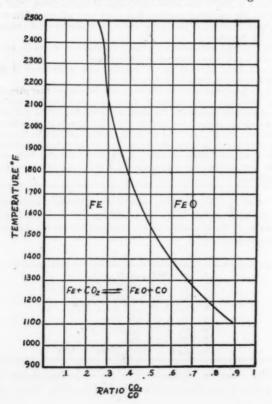


Fig. 1

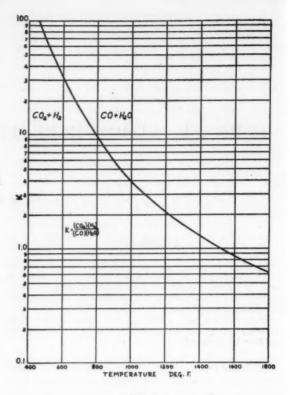


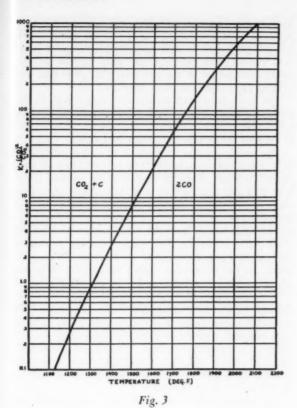
Fig. 2

tors, H_2O and CO_2 are in the numerator of the fraction establishing the constant. Therefore, by lowering the H_2O and the CO_2 , it is possible to stay to the left of the curve and a safe distance from the equilibrium condition.

However, with the combined effect of water vapor and carbon dioxide, CO2 is in the numerator and H2O in the denominator. Since it is desirable to maintain a low concentration of both CO₂ and H₂O, it becomes necessary to stay as close to the equilibrium as possible. An increase in H2O must be accompanied by a corresponding increase in H2 and the same holds for an increase in CO2 which calls for an increase in CO. This system approaches equilibrium at 1700 F when K = 0.73 and at 1350 F when K = 1.5. With a type 102 atmosphere and a 70 F dew point, the equilibrium constant K == 3.07 at which figure the reaction will be to form CO and H2O at temperatures above 1100 F. With a 36 F dew point K = 10.7 which is still further away from the equilibrium line at normal annealing temperatures.

Eq. 4 and 5—Carbon Dioxide and Carbon and Iron Carbide (Fig. 3)—Both equations involve the development of CO by the action of CO_2 on carbon. Thus a fifth variable, carbon, is introduced. The system CO_2 -C-CO involves the square of the CO in relation to the first power of CO_2 . The constant which establishes the curve is represented by $K = \frac{(CO)^2}{CO_2}$. Since there is

carbon in white iron, this reaction becomes a factor in increasing the CO content as long as there is CO₂ present, either introduced as part of the generated at-



mosphere or as a result of the introduction of air into the furnace.

Formation of CO by this reaction, increasing the total CO percentage, will lower the product constant of the CO₂-H₂-CO-H₂O system with a tendency to form CO₂ and H₂. Then, with the additional CO₂ formed, there is a progressive chain reaction of CO₂ combining with carbon in the iron to further decarburize the casting.

Eq. 6 and 7—Methane, Carbon and Iron Carbide—Unless appreciable amounts of raw gas are deliberately put into the furnace chamber, the per cent of methane is a minor factor. With a Type 102 atmosphere, the methane content is about one-half per cent. The constant upon which the curve is established for the reaction $CH_4 - C + 2H_2$ is CH_4 divided by the second power of the hydrogen content. CH_4 . Thus the

constant for the Type 102 gas, as generated, 0.5 per cent methane, 15 per cent hydrogen becomes 0.0022 at which concentration methane is stable. The constant for this system ranges from 0.025 at 1700 F to 0.1 at 1400 F. If methane is introduced to the extent of 5 per cent, the constant becomes 0.022 at which concentration the system CH₄-C-2H₂ is close to equilibrium at 1700 F. With an atmosphere containing 15 per cent hydrogen the addition of 5 per cent methane is without significant effect.

Addition of 5 per cent methane may be accomplished by adding natural gas which is practically 100 per cent hydrocarbons. However, if coke oven gas, which may contain 50 per cent hydrogen and 30 per cent methane, is introduced, not only is the equili-

brium of the four principal variables disturbed, but the reaction will tend to proceed to the left, forming methane and taking carbon from the casting.

Eq. 8—Iron Oxide and Iron Carbide—As ferrous castings have a more or less continuous scale consisting of iron oxide, it is necessary to consider the effect of iron oxide in the furnace chamber. Not only does iron oxide react with iron carbide to form CO but iron oxide is reduced by CO and by hydrogen to form CO₂ and water vapor. It therefore may react to increase the amounts of three of the four principal variables.

Atmospheres in general use, both those externally generated and that developed in the furnace chamber, vary considerably in per cent of combustibles, principally carbon monoxide and hydrogen. As such atmospheres are inflammable, they are therefore explosible as the difference between these two phenomena is one of physical confinement rather than of the specific mixture of the gas.

It is advisable to take precautionary measures in operation of the furnace, to avoid conditions favorable to explosions. This is essentially a matter of avoiding a situation in which an appreciable volume of combustible gas is heated to the ignition temperature of the mixture.

If the furnace is brought up to a good red heat (1400 F) combustible atmosphere may be put into the furnace chamber, the gas burning slowly until the amount of oxygen available is below that with which the mixture of gases is inflammable.

Such a practice applies to the regular operation of a periodic furnace or when starting up a continuous furnace. The procedure is usually to tie in the opening of the valve which admits the atmosphere, to a thermocouple controlling and recording the temperature in the furnace. Thus the purging of a furnace with a combustible atmosphere is best done at temperatures of 1400 F and over. The time required to purge a furnace depends upon the volume of atmosphere, expressed as the percentage of furnace volume, admitted in a given period of time, and the temperature of the furnace. Experience has shown that purging at the rate of 40 per cent of furnace volume per

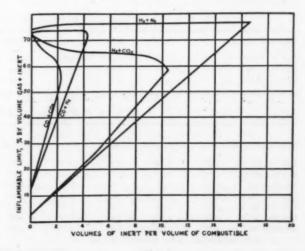


Fig. 4

hour for a period of 4 hr gives a satisfactory furnace atmosphere, the exchange or purge being about 95 per cent complete. This is with a combustible atmosphere.

With Type 201 noncombustible atmosphere, it is possible and safe to purge a cold furnace. This will provide protection against oxidation during the period in which the castings are heated to the holding tem-

perature.

As the limits of inflammability for combustible gases have been established, it is possible to establish limits for a mixture of combustibles and inert gases by applying the so called "mixture-rule" (first used by Le Chatelier) from the formula shown below and the curves shown in Fig. 4.4

$$Lower\ Limit = \underbrace{\frac{100}{\underbrace{H_a + N_a}_{P} + \frac{CO + CO_a}{P}}}$$

 $P \equiv$ the point on the pertinent curve corresponding to the ratio of inert to combustible.

The same formula may be used to compute the upper limit selecting the higher points of intersection on the curve. The inert CO_2 and N_2 may be apportioned with different combustibles in any way convenient but the ratio should be such that there will be a corre-

sponding point on the curve.

Type 201 containing 2 per cent hydrogen and 2 per cent monoxide requires only 41.4 per cent nitrogen to render the mixture noninflammable. This is based on a 16 to 1 nitrogen to hydrogen ratio and a 4 to 1 ratio for the carbon monoxide. Since the atmosphere as produced contains 96 per cent nitrogen, no mixture of this atmosphere with air will be combustible.

Type 102 and the atmosphere generated in the furnace chamber show about the same limits of inflammability that is 23 per cent and 73 per cent.

In order to determine to what extent a low carbon monoxide atmosphere would decarburize or possibly carburize, two series of tests were made.

In the first test, steel bars of various carbon content were selected. The bars were 1½ in. round turned down to 1 in. to remove the as-rolled surface. They were put through an 80-hr annealing cycle in a fur-

nace atmosphere of 7 to 8 per cent CO and 4.5 to 5 per cent hydrogen. It was proposed to establish the "carbon pressure" of such an atmosphere and a practical equilibrium between the solid carbon in the metal and carbon in the gases for this particular annealing cycle from the data obtained.

The procedure with respect to analysis was to determine chemically the carbon content in consecutive layers from turnings removed from the bar. The results of analysis for carbon are shown in Table 3.

The results are the averages of two bars, duplicates as to metal and treatment. The two 0.46-carbon bars checked within limits of error for the method used. The 0.24 and 0.61-carbon bars showed greater differences on individual analysis than could be accounted for by experimental error. The differences were consistent, one bar in each set being about ten points higher than the other. The results from the first turnings are low possibly because of secondary decarburization as the bars were cooled from 1300 F in air when the load was discharged from the furnace.

The chemical analyses show a fairly consistent trend. Because of the long annealing cycle, all of the bars showed a coarse grain. For this reason it was difficult to distinguish areas or zones of higher or lower carbon content in the microstructure. The bars were normalized and with a uniformly fine grain it was possible to confirm in a general way the differences in carbon shown by chemical analysis.

Disregarding the heat up and fast cool periods the 80-hr cycle is essentially a matter of about 26 hr at 1700 F and 38 hr between 1500 F and 1300 F. This period may be considered as averaging 1400 F although it is recognized that the carbon equivalent for CO-CO₂ atmospheres is much higher at 1500 F than at 1300 F.

While the atmosphere generated is essentially free of carbon dioxide and the furnace atmosphere shows only traces, for purpose of comparison a CO₂ content of 0.20 per cent is used with an 8 per cent CO as representative.

Work of several investigators seems to agree on carbon equilibrium constants for CO-CO₂ atmospheres at various temperatures.^{5,6} The carbon equilibrium for

TABLE 3-RESULTS OF STEEL BAR ANALYSES, % C

| Depth of Cut, in. | 0.0025 | 0.005 | 0.0075 | 0.010 | 0.0125 | 0.015 | 0.020 | 0.025 | 0.030 | 0.035 |
|-------------------|--------|-------|--------|-------|--------|-------|-------|-------|-------|-------|
| Bar A (0.61% C.) | 0.45 | 0.60 | 0.61 | 0.62 | 0.62 | 0.60 | 0.57 | 0.56 | 0.54 | |
| Bar B (0.46% C.) | 0.44 | 0.58 | 0.57 | 0.56 | 0.55 | 0.60 | 0.59 | 0.57 | 0.53 | 0.48 |
| Bar C (0.24% C.) | 0.23 | 0.45 | 0.46 | 0.43 | 0.41 | 0.40 | 0.36 | 0.34 | 0.32 | 0.29 |

TABLE 4-RESULTS OF MALLEABLE BAR ANALYSES

| Depth of Cut, in. | 0.0025 | 0.005 | 0.0075 | 0.010 | 0.0125 | 0.015 | 0.020 | 0.025 | 0.030 | 0.035 | Center Drilling |
|-------------------|--------|-------|--------|-------|--------|-------|-------|-------|-------|-------|--------------------|
| Bar No. 82 AC | 0.70 | 0.78 | 0.82 | 0.96 | 1.00 | 1.20 | 1.22 | 1.26 | 1.62 | 1.70 | 2.28 |
| Bar No. 22 AC | 0.64 | 0.68 | 0.70 | 0.86 | 1.18 | 1.34 | 1.42 | 1.54 | 1.98 | 2.06 | 2.24 |
| Average AC | 0.68 | 0.73 | 0.76 | 0.91 | 1.09 | 1.27 | 1.32 | 1.40 | 1.80 | 1.88 | 2.26 |
| Bar No. 81 M | 1.02 | 1.18 | 1.62 | 2.08 | 2.10 | 2.10 | 2.12 | 2.14 | 1.6 | | |
| Bar No. 82 M | 1.22 | 1.28 | 1.40 | 1.72 | 1.94 | 2.00 | 2.04 | 2.08 | | | |
| Bar No. 22 M | 0.96 | 1.22 | 1.44 | 1.76 | 1.96 | 2.08 | 2.10 | 2.12 | | | |
| Average M | 1.07 | 1.22 | 1.48 | 1.85 | 2.00 | 2.06 | 2.08 | 2.11 | | | |

Bars marked AC were annealed as cast.

Bars marked M were machined prior to annealing.

Readings in per cent carbon.

an atmosphere containing 0.20 per cent CO₂ and 8 per cent CO is 0.80 per cent carbon at 1400 F, 0.40 carbon at 1500 F and 0.20 at 1600 F. For temperatures above 1600 F the point falls considerably to the left of the 0.20 equilibrium line. At 1700 F this atmosphere would be expected to decarburize to a carbon content less than 0.20. At temperatures below 1600 F the reaction should be to restore or retain carbon up to a certain percentage depending upon the temperature and to a certain depth depending upon time and temperature.

From data shown in Table 3 it appears that the combined or cumulative influence of this atmosphere in this specific cycle is to come to an equilibrium at a carbon content of about 0.55 or 0.60 per cent.

There are some discrepancies and variations not accounted for by factual data. However, none of the bars showed a total decarburization, that is, a surface layer of ferrite greater than 0.002 in. and carbon content as determined chemically is substantiated by the microstructure.

A second test was made using hard iron bars. The bars were 1 in. round and 6 in. long. Four bars were ground to a depth of 0.040 in. to remove oxide and surface metal. The ground bars and four as cast bars were put through the 80-hr annealing cycle in the same box with the steel bars. Because of warpage and a slight shift only five of the eight bars were suitable. Analyses for carbon were made of consecutive sets of turnings. These results are shown in Table 4.

Purpose of Test

The purpose of this test was to determine to what extent the normal casting surface affects the character of the rim and to investigate the action of a high nitrogen atmosphere on the metal with the variables due to surface conditions eliminated. The carbon content of the machined bars seems to have leveled off at about 2.00 per cent and at a depth of 0.015 in. The carbon at a depth of 0.025 in. is only 16 points lower than drillings from the center of the bar.

Bars annealed as-cast are 30 to 96 points lower in carbon than the machined bars at the corresponding depth of cut. At a depth of 0.035 in. one bar is 58 points lower than the center drillings. The carbon differential is not uniform and the increment is greatest between the sixth and seventh cut.

The annealed microstructure with irregular size and distribution of graphite nodules does not lend itself to estimation of carbon content except as a general approximation. There are fewer graphite nodules near the surface and the microstructure may be considered as being in general agreement with the findings by chemical analyses. The correlation is far from that which is possible with properly normalized steel bars.

The hard iron bars were examined to determine to what extent they were decarburized since the as cast bars were much lower at the surface after annealing than those which had been turned down.

Under the microscope there was no definite zone in which there was no cementite, that is, no layer of a purely eutectoid composition. There was an occasional grain of ferrite, some graphitic areas, some iron oxide attached to the surface, but no indications of a progressive carbon differential. Two hard iron bars were touched up with a file and an effort made to turn off in steps of 0.005 in. in depth as was done with annealed bars. The results were not satisfactory with light cuts. With the equipment available it was necessary to make the first cut 0.012 in. deep. Carbon determinations of these turnings gave results of 1.74 and 1.76 per cent. These results indicate that some of the metal in the outermost 0.012 in. is low in carbon but offer no accurate conception of the carbon gradient.

Using results shown in Table 4, the carbon in the first 0.015 in. of the bars annealed as-cast is about 0.94 per cent. This is considerably lower than the 1.74 per cent obtained on 0.012 in. turned from the hard iron bars.

The data at hand seems to warrant the statement that the carbon gradient that exists on the bars annealed "as cast" is due principally to loss in carbon during the anneal and to a lesser extent to a condition which exists in the hard iron before annealing. The exception to this has been discussed and illustrated in Fig. 9 and 10.

Loss of Carbon

Because of the relatively small amount of iron oxide in the scale of a casting it is hardly conceivable that the greater loss of carbon formed on the bar annealed as-cast is due to the action of oxygen present as iron oxide. Theoretically this is a factor (Eq. 8) but not of too much significance. The concept is offered that the greater loss in carbon may be attributed to a greater surface resulting from the normal irregularities of a sand cast surface.

When castings are annealed in sealed pots some modification of the "atmosphere" or environment in the pots may be accomplished by the selection of the packing material. When no packing is used, little can be done to alter conditions in the pot.

However, when no sealed unit containers are used, such as in the muffle type, radiant tube or electrically heated furnaces, a suitable atmosphere must either be generated in or introduced into the furnace chamber.

A suitable or optimum atmosphere is not only protective against excessive oxidation and decarburization but should enhance graphitization; certainly not act to retard the process. It is possible to load and seal a periodic type furnace and to develop an atmosphere in the furnace, usually containing more than 30 per cent carbon monoxide which will avoid excessive oxidation and decarburization of the castings. In a continuous furnace which may be opened at intervals of 30 to 150 min, it is necessary to maintain a suitable atmosphere and to provide means to prevent or counteract the introduction of air during the loading and unloading intervals. In either type, batch or continuous, the furnace should be tight and the construction such that a positive pressure may be maintained by pumping a minimum quantity of gas, preferably just enough to overcome the losses when the furnace is open for loading and unloading.

Type of equipment may be put into two groups:

1. Periodic or batch type. This includes the refractory muffle, the bell or cover furnace and the box type

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which may be gas-fired radiant tube or electrically heated, the two chamber elevated furnace, the conventional door type or the elevated furnace with openings in the bottom through which castings are put into the furnace by means of an elevator and a pusher mechanism.

2. In the second group are the continuous furnaces, the pusher or the driven rolled type with side, end or bottom openings through which the castings are put into the furnace.

Because periodic furnaces are sealed for the duration of the annealing cycle, it is possible to develop a furnace atmosphere such that the surface condition of the castings is commercially acceptable.

In Fig. 5 is shown a surface or rim typical of that resulting from this type of anneal. This casting was taken from a 16-ton charge annealed in 69½ hr, the cycle being essentially as follows:

Heat up to 1750 F
Hold at 1750 F
Fast cool to 1350 F
Slow cool to 1250 F
Cool to 1050 F

8½ Hr (Hot Furnace)
26 "
(3½ deg per hr)
2 " (To handle)

69½ Hr

The casting shows a fully annealed structure and the composition is representative of cupola air furnace duplex iron with a silicon content of 1.15 to 1.20 per cent. Because of the mass of the charges there is a lag in temperature of perhaps 100 deg at the start of the slow cool period. The furnace atmosphere in this operation will show carbon monoxide to the extent of 65 per cent in the high temperature holding period. decreasing to 8 or 10 per cent toward the end of the slow cool period with a corresponding increase in carbon dioxide as the atmosphere collapses and air is admitted. Data presented in this paper are such as to indicate that the castings may be recarburized. It may be in this case, the carburizing effect of the high CO is such that it is necessary to cool at the rate of 31/6 deg per hr in order to finish the anneal without a heavy

pearlitic rim. Effect of excessive moisture in the furnace atmosphere is illustrated in Fig. 6, 7 and 8. Castings annealed showed a rim about $\frac{3}{32}$ in. thick, the outer $\frac{1}{32}$ in. was ferritic and the inner $\frac{1}{16}$ in. was pearlitic. The castings were annealed in a periodic furnace

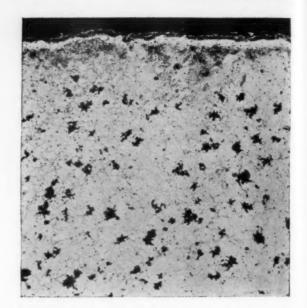


Fig. 5

which had recently been repaired and had not been thoroughly dried. No determinations were made of the atmosphere but presence of moisture was established from the fact that the furnace steamed during the first part of the heat plus the information that 65 gal of water had been used in the mortar in setting up the brick. The annealing cycle was essentially the same as the 69½-hr cycle shown above.

A satisfactory anneal is obtained in a periodic furnace purged with a Type 102 atmosphere. The practice has been to purge in 4 hr and thereafter to use enough atmosphere to maintain a pressure 0.02 in. to 0.04 in. of water. The cycle, annealing a 15-ton charge of 1.00 silicon iron casting, is about 56 hr.

Heat to 1725 F
Hold at 1725 F
Cool to 1480 F
Cool to 1300 F

10 Hr (Hot Furnace)
21 "
5 "
20 " (8 deg per hr)
56 hr

With a well sealed furnace the CO will be about 34 per cent as a maximum and hydrogen 15 per cent.

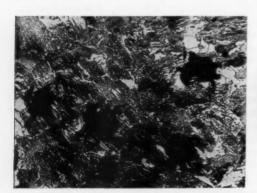


Fig. 7-Rim of specimen in Fig. 6. Nital etch. Mag. 125 x.



Fig. 6-Actual Size

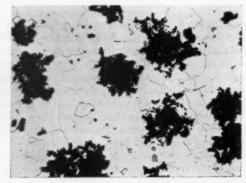


Fig. 8-Core of specimen in Fig. 6. Nital etch. Mag. 125 x.

As the word "excessive" implies a safe working maximum, it should be added that moisture should be correlated with time at temperature. A Type 102 atmosphere with a 70 F dew point has given satisfactory results in a 14½-hr anneal of high silicon (1.60 per cent) castings. It was considered necessary to lower the dew point to 36 F for cycles 26 hr and over.

The pearlitic rim or "frame" on a malleable casting is quite generally attributed to conditions existing during the anneal. Because of the length of time above a temperature at which oxidation takes place, this is a logical conclusion borne out by experience. However, there are conditions under which considerable oxidation of the hard iron may occur.

A considerable amount of oxidation may result when a mold is broken. This is particularly true with a large casting when only part of the casting is exposed. The remainder being covered retards the cooling with the result that the exposed portion may be red for 20 or 30 min. A small casting which cools normally in the mold or when dumped hot cools rapidly, showing only a relatively thin scale and practically no decarburization.

In Fig. 9 is shown the microstructure of the metal at the surface of the flange of a 25-lb casting. The flanged end was exposed by partially breaking the mold and was at a red heat for about 30 min. There is complete decarburization to the extent of 0.027 in. and a subjacent pearlitic area to a depth of 0.031 in. The flange section is $\frac{7}{8} \times 4 \times 6$ in.

The microstructure at the surface of small (3/4-lb) casting through a 3/4-in. round section which cooled normally in the mold is shown in Fig. 10. Both the heavy and the light castings were poured from a 1.00

silicon iron annealed in an 80-hr cycle.

Atmosphere in the furnace runs consistently 7 to 8 per cent CO and 4.5 to 5 per cent H₂. The furnace atmosphere is higher in CO than that coming from the generator which runs 2 per cent CO and 2 per cent H₂. The increase in CO is the result of oxidation of carbon (Eq. 8) and increase in hydrogen results from reduction of water vapor. (Eq. 1 and 3.) The atmosphere leaving the generator shows a dew point of about 34 F and about 0.7 per cent water vapor. The dew point on the furnace atmosphere is lower than that of the generator atmosphere, indicating that some of the water vapor has been reduced to hydrogen.

Effect of excessive moisture has been discussed and illustrated in Fig. 6, 7 and 8. A similar surface condition may result from contamination of the furnace atmosphere by air. This is illustrated in Fig. 11. The rim is a little less than 1/16 in. deep consisting of about 0.010 in. of ferrite and about 0.040 in. of pearlite. The structure shown was typical of castings annealed in this operation. A Type 102 atmosphere with a 36 F dew point was introduced at the rate of about 15 per cent of furnace volume per hour. Difficulty was encountered in holding a positive pressure on the furnace even at a much higher rate of flow. Investigation established that the chamber was not sufficiently well sealed with the net result that an appreciable quantity of air was being drawn into the furnace. The atmosphere in the furnace varied considerably showing 2 to 5 per cent CO₂, 12 to 14 per cent CO and about 15 per cent hydrogen.

When a Type 201 atmosphere was used, the structure at the surface of the casting was the same as that which prevailed with the Type 102 atmosphere.

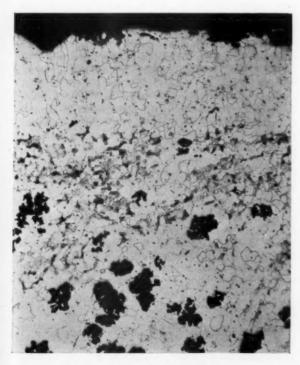


Fig. 9-Nital etch. Mag. 50 x.

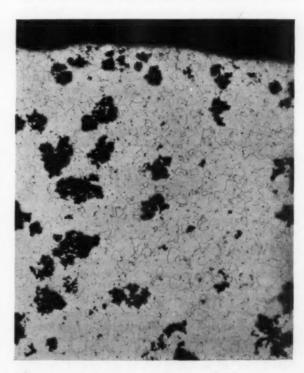


Fig. 10-Nital etch. Mag. 50 x.

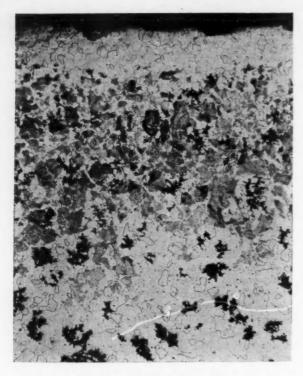
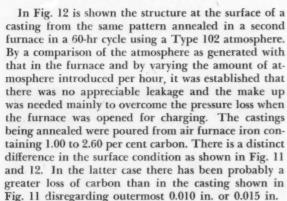


Fig. 11-Nital etch. Mag. 50 x.



In one there has been persistence of pearlite under like conditions of cooling for second stage graphitization and with the same discharge temperature. The conditions of operation and the type of rim shown prevailed over a period of several months and are representative. The indications are that the more or less direct oxidation of carbon by air, like water vapor, tends to form the pearlitic type of rim.

To determine the character of a rim resulting from a "low carbon pressure" atmosphere castings were annealed in a periodic furnace using a 36-hr cycle. The furnace was purged with a Type 201 atmosphere at the rate of 40 per cent of furnace volume per hour throughout the entire cycle. Bleeder outlets were adjusted to maintain a positive pressure on the furnace of 0.02 in. to 0.03 in. of water.

The maximum CO content was 7.5 per cent obtained after 19 hr which was at the end of the high temperature holding period. At the end of the 4-hr

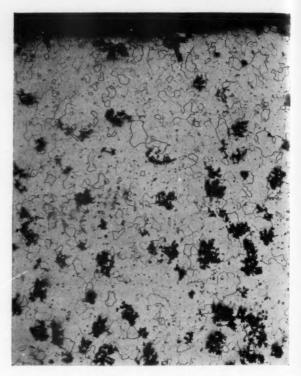


Fig. 12-Nital etch. Mag. 50 x.

fast cool period the CO was 3 per cent. During the 13-hr slow cool period the CO content was 2 per cent which is the same as that of the atmosphere leaving the generator.

The structure at the surface of a casting from this heat is shown in Fig. 13. There is a completely ferritic rim to a depth of 0.015 to 0.020 in. There is partial loss of carbon to the extent of an additional 0.020 in. in which zone there are traces of feathery pearlite. This decarburization has taken place in a practically complete absence of CO2 and O2 as such. Water vapor was present to the extent of 0.7 per cent introduced with the atmosphere. Because the rate of flow is about three times that normally used, the moisture content is higher and maybe a significant factor. However, this surface condition was produced in an atmosphere of a low CO content and therefore of low carbon pressure. There appears to be no tendency to retain pearlite or to set up conditions in which pearlite resists graphitization.

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The conception that a pearlitic rim may result from the action of direct decarburization by CO₂, O₂ and/or H₂O and that the ferritic rim results from decarburization caused by low carbon pressure appears to be supported by the data presented.

It is logical also to anneal in an atmosphere containing carbon monoxide which reacts to inhibit the action of carbon dioxide on carbon and which with increasing amounts sets up a higher carbon potential in the atmosphere.

Since carbon monoxide is a desirable constituent, it becomes then a matter of determining how much carbon monoxide is necessary to obtain a satisfactory surface condition.

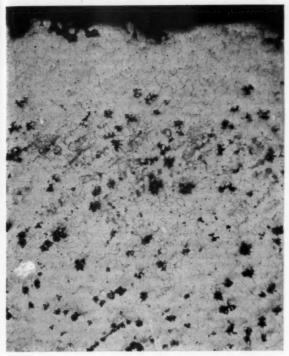


Fig. 13-Nital etch. Mag. 50 x.

Tests made using steel and hard iron bars, the results of which are shown in Tables 3 and 4, indicate that 7 or 10 per cent carbon monoxide is sufficient to obtain a minimum of 50 points of carbon at the surface of a casting under the conditions of time, temperature and atmosphere as recorded above.

It has been the intention to confine the data and observations to the various types of atmospheres and the reaction of their components with particular reference to the effect on the surface of castings during the annealing operation. It is hoped that this will be of some value to those operating controlled atmosphere annealing equipment and that the data will stimulate further investigation of this as well as other phases of the heat treatment of white cast iron.

Acknowledgment

Assistance of Peter Heich, M. J. Rensink and other members of the Metallurgical Department in the preparation of this paper is gratefully acknowledged.

Summary and Conclusions

Development of a satisfactory surface condition on malleable castings is important for several reasons. A heavy pearlitic rim is undesirable because of poor machinability and because such castings may crack in forming or in service.

A fully ferritic rim permits considerable amount of forming without fracture, a condition which is usually reflected in a high percentage of elongation on the test bars. Such a condition is not good for machining, particularly threading and tapping. For this reason the surface metal should contain some temper carbon and should not differ greatly from the core of the casting.

A certain amount of surface decarburization of iron of the normal carbon content appears to be unavoidable. Oxygen, carbon dioxide and water vapor at the temperatures required for a practical annealing cycle are actively oxidizing to carbon. If it were possible to maintain an atmosphere in which carbon monoxide, carbon dioxide, hydrogen and water vapor were in equilibrium at any and all temperatures above 1300 F there would still be some decarburization because of a low carbon potential in the atmosphere.

If the carbon monoxide were increased in the interests of raising the carbon potential of the atmosphere, the equilibrium of the four principal constituents would be disturbed. Equilibria vary with temperature. An atmosphere which is decarburizing at 1700 F may be carburizing at 1400 F.

In order to avoid excessive decarburization and the attendant possibility of developing a pearlitic rim, it is logical to use an atmosphere which contains a minimum of oxygen, carbon dioxide and water vapor since these are active decarburizers.

References

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- 2. American Gas Association, Industrial and Commercial Section, Information Letter No. 9.
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- 4. Gaesous Fuels, p. 236 (1948), published by American Gas Association.
- 5. Holcroft and Bayer, "Control of Carbon Content During Heat Treatment of Steel in Atmosphere Furnaces," *Materials and Methods*, Aug. 1948.
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DISCUSSION

Chairman: F. W. JACOBS, Lake City Malleable Iron Co., Ashtabula, Ohio.

Co-Chairman: R. P. Schauss, Illinois Clay Products Co., Chicago.

R. M. CHERRY (Written Discussion): Mr. McMillan certainly deserves a vote of thanks for the excellent work that he has done in connection with the preparation of this paper.

Heat treatment of high carbon iron or steel presents the primary problem of preventing or retarding the process of decarburization. The most suitable atmosphere then is one which eliminates the decarburizing gases CO₂ and H₂O.

Reducing of the H₂O only offers very little improvement in carbon potential since, with CO₂ present, the formation of water will occur in the furnace chamber due to reformation, water gas reaction, Equation 3.

A prepared atmosphere of either type 201 (high ratio neutralene) or 202 (low ratio neutralene), both of which are CO₂-free, will give the most protection from decarburization.

In either case, from a practical standpoint, it is difficult to establish an atmosphere with a carbon potential above 1.0 carbon. This means then that there will still be some decarburization on iron with 2.0 carbon content.

Our experience has indicated that the best atmosphere is that which is generated by the iron itself during the annealing cycle. To maintain this self generated gas, a furnace must really be tight. Even a thermocouple tube not tightly packed, especially when located in the top of the furnace casing, can leak out the atmosphere fast enough to reduce the CO and increase the CO₂ due to inward leakage of air.

It is entirely practical to build furnaces of the required tightness in both batch and continuous types and many electrically heated furnaces of both types are producing satisfactory results without the use of any external source of protective atmosphere.

¹ Mgr. of Sales, Industrial Heating Divisions, General Electric Co., Schenectady, N. Y.

The following is the analysis of gas atmosphere taken from electric elevator furnaces used for the annealing of malleable iron. The installation consists of two furnaces, one a high temperature furnace for the high temperature portion of the cycle and the second furnace for the second portion of the cycle. After the charge has been heated to the high temperature and after completion of the holding time, then the charge is withdrawn from the furnace and immediately placed in the low-temperature furnace where the charge is equalized by approximately 1380 F and then slow cooled.

The atmosphere in the high temperature furnace, after the charge was brought to the temperature of approximately 1725 F was CO, 52.2 per cent; CO₂, 3.2 per cent; O₂, 0 per cent.

In the low temperature furnace, after equalizing, the analysis showed CO, 20 per cent; CO₂, 14 per cent; O₂, 0 per cent.

A. H. RAUCH and E. WELANDER² (Written Discussion): Mr. McMillan is to be congratulated on his thought-provoking paper on furnace atmospheres for malleable annealing. We have had some experience with furnace atmospheres which are not covered in Mr. McMillan's paper. It has been our experience that hydrogen in excess of approximately 10 per cent retards graphitization, especially during second stage.

When we first went to continuous short cycle annealing we found it impossible to anneal within any reasonable length of time when using an atmosphere containing 35 to 40 per cent hydrogen. When we discontinued this atmosphere we were able to employ the short cycle which we had anticipated with the equipment. We also found, however, with our present CO to CO₂ atmosphere, that if the hydrogen content climbs to over 10 per cent we have trouble due to incomplete second stage graphitization. Previously published information by Schwartz and others indicates that hydrogen is highly detrimental to annealing when present either in the metal or in the atmosphere. Quite possibly the amount of hydrogen in the atmosphere required to retard graphitization is less when the original hydrogen content of the metal is high.

We also found the ratio of CO to CO₂ to be important. We found that we could not anneal in the desired time cycle with an atmosphere rich in CO. We then tried annealing in air and found that the cycle required was short but the castings had scaled badly. We finally found that we could eliminate scaling and still anneal in a reasonable length of time by employing an atmosphere having a CO:CO₂ ratio of 1:1 to 2:1. Whenever the ratio exceeds 2:1 we encounter some difficulty with incomplete second stage graphitization. We obtain only a light rim of total decarburization with an occasional trace of pearlite below the rim

The atmosphere employed must be a compromise between

what is metallurgically ideal and what is commercially feasible. The presence of temper carbon nodules at the surface is a metallurgical ideal which may not be necessary to attain in practice. The light rim of total decarburization with occasional traces of pearlite below the rim has not been found to cause any trouble in machining. We find it unnecessary and economically inadvisable to try to secure surfaces free of decarburization.

L. F. PORTER (Written Discussion): In his discussion of the effect of furnace atmosphere on the structure of the iron surface, Mr. McMillan observed that water vapor as well as oxygen and CO₂ produced a pearlitic rim. This is an important observation which deserves further clarification.

It should be recognized that either one of two elements is responsible for the observed phenomena. The two elements are hydrogen and oxygen. Hydrogen produces a thin decarburized surface followed by a thick layer of stabilized pearlite. Oxygen produces a relatively thick decarburized surface followed by a very thin pearlitic area. The difference in the thickness of the affected areas is believed to be associated with the difference in the rates of diffusion of hydrogen and oxygen in the metallic matrix.

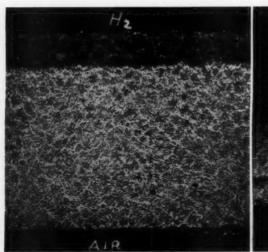
The effects described above are nicely illustrated in the following two macrographs made of ½-in. sections of gray cast iron. The iron was placed in a special gas chamber so that one surface was flushed with an atmosphere of pure dry hydrogen, while the other was in contact with air. The first specimen was then heated to 1700 F, and held for 2½ hr, cooled to 1400 F and held 2 hr, and finally air cooled to room temperature. The second specimen was held in gas chamber for 21 hr at 1700 F.

The composition of the gray iron was 3.5 C, 2.6 Si, 0.55 Mn, 0.75 P and 0.07 S. This iron anneals rapidly, forming a completely ferrite matrix even on very short holding times at subcritical temperatures. Yet, as may be seen in the macrographs, the dry hydrogen flushed surfaces show heavy rims of stabilized pearlite and slight decarburization at the surface. The surface exposed to air shows a very thin pearlitic rim and a heavy decarburized area in the case of the 21-hr treatment and no apparent pearlitic rim in the case of the 2-hr treatment.

It is believed that the strong stabilizing effect of hydrogen on the carbide is not generally recognized, and that it might be of considerable importance when attempting to anneal malleable cast iron using atmospheres containing appreciable quantities of hydrogen gas or hydrogen containing compounds.

MR. McMILLAN: I would like to thank Mr. Cherry for his discussion. As indicated in the text of this paper, with a tight furnace it is possible to develop an atmosphere which will give an acceptable surface condition on the castings. This is usually a batch-type furnace. For the most part continuous furnaces require the introduction of an externally prepared atmosphere.

21/2-hr Treatment



Nital Etch









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21-hr Treatment

⁴ Research Metallurgist, The University of Wisconsin, Madison, Wis.

² John Deere & Co., Moline, Ill.

³ Union Malleable Iron Works of John Deere & Co., E. Moline, Ill.

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With regard to what Mr. Rauch has said, it is just full of things that should be talked about. The hydrogen story is probably not hydrogen as such but the fact that with hydrogen and CO, water vapor becomes a factor. I cannot quite understand how the presence of hydrogen in the atmosphere can stabilize pearlite. Hydrogen in the metal picked up during melting may stabilize cementite. Hydrogen may be oxidized to water vapor which can oxidize the metal and retard second stage anneal. Dew point readings to determine the amount of water vapor should be available as additional data when considering the effect of hydrogen.

The reason high CO content may give trouble is not due to the presence of a considerable amount of CO but because the CO was developed in the first stage of the anneal and reflects a condition of oxidation of carbon during the first stage. High CO indicates high loss of carbon in the first stage. Thus with a lower carbon content in the surface, it becomes necessary to lengthen the time in the second stage to overcome the condition set up in the first stage.

In other words, you lose ability to graphitize due to conditions set up in the first part of the cycle and therefore you penalize yourself.

I would like to mention one phase of this general subject of atmosphere which is in the text and that is the explosive limits and inflammability.

In the last issue of *Industrial Heating* there is an article by J. B. Smith, I believe from Factory Mutual, in which he mentions that there have been 27 explosions in furnaces using highly combustible atmospheres. Factory Mutual is very much interested in explosions and it may be that those of us operating this type of equipment using combustible atmospheres should also be concerned about the explosion hazards that exist.

The atmosphere we are using containing 2 per cent hydrogen and 2 per cent CO would have a heat value of about 12 Btu if the gas could be burned. This gas results from combustion of 800 Btu gas with 93 per cent aeration. Any products of combustion using 83 per cent or more aeration are pretty safe from the standpoint of producing a combustible gas.

While the type 201 atmosphere is not combustible, the gas in the furnace due to an increase in both CO and hydrogen becomes combustible under certain air-gas ratios. The CO may increase to about 8 per cent and the hydrogen increase to about 5 per cent. The CO content may be varied by varying the amount of atmosphere introduced into the furnace per hour. If enough atmosphere is introduced per hour, that is, if the rate of introduction is high enough, the analysis of the furnace atmosphere will be same as that of atmosphere generated.

The 8 and 5 combination has a Btu value of about 40. This will burn with difficulty. The Type 102 has about 73 Btus. The atmosphere developed in a closed furnace, about 206 Btus. The 8 and 5 atmosphere will not furnish enough heat to maintain

combustion.

There is another phase which was not covered in the paper but may be of interest. The total gas consumption for heating and atmosphere is about 1800 Btus per pound of casting. This is based on meter readings and the weight of castings going over the scale.

I would like to say in closing that we feel it is extremely important to avoid the use of atmosphere which has a high Btu value. For an operation on a 24-hr, 7-day basis, we feel that the low Btu atmosphere provides a much safer and healthier condition.

R. W. Heine: The subject of reactions between gases and furnace atmosphere and metals is a very complex one, and Mr. McMillan is to be congratulated for embarking upon a subject as complex as this one is. I have been interested in the subject of deoxidation of cast irons, and I have an experience which I would like to recite.

If a series of tensile test bars made of deoxidized white iron are annealed in periodic ovens, packed in boxes, with gravel, or if they are annealed in an electric furnace of the short cycle type, differences in the surface layers are noted which seem to be a function of the condition of the metal rather than the furnace atmosphere. In a series of test bars in which a deoxidizing addition is made to the molten metal, either of aluminum or titanium, as the addition increases, the fracture of the test bar changes from the normal all over black appearance to one in which a definite white ring appears at the edges. Examining that white ring, we found there was considerable decarburization in evidence, as revealed by a marked decrease in the number of nodules in the ring. Since I have not pursued this particular observation further I am wondering if possibly the character of the metal, in addition to that of the atmosphere, does not have a very pronounced influence on the nature of the surface layer which is developed during the annealing cycle.

Mr. McMillan: We have three types of iron, a 1.60-Si iron with a 14-hr anneal, a 1.25-Si duplex iron and a 1.0-Si cold melt iron. All are annealed under this type of atmosphere.

We have a lower graphite count with the lower silicon—20 to 25 nodules per sq mm with a 1.0-Si iron and about 50 with the 1.6-Si iron. However I would not venture a comment on the composition of the metal having an effect on the rim, after you have added aluminum and titanium. That rim, if it was pearlitic, would depend upon what you did with annealing cycles. If you anneal long enough, you can get a graphitized rim providing you do not have too much CO or decarburization. It does not take much CO. We can increase the carbon in a piece of steel from 0.25 to 0.40 and it has no silicon, so you must not confuse that with a metal that has 1.5 Si, which definitely retards the rate of carburization.

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BASIC-LINED CUPOLA FOR IRON MELTING

By

Sam F. Carter*

ABSTRACT

Successful production experience on a small cupola with a basic lining is described with comparison of metal chemistry, slag chemistry, and refractory consumption.

Twenty experimental heats with intentional oxidation indicated several ways to remove from 0.10 to 0.32 per cent P from charges of 0.30 to 0.80 per cent P content. Some slag analyses are given.

Seventy experimental heats with various fluxes were directed toward desulphurization. Effect of slag composition on sulphur distribution was indicated. Special flux combinations removed as much as 0.187 per cent sulphur from high sulphur charges and produced sulphur as low as 0.010 to 0.030 per cent directly from the cupola.

ONE OF THE PRINCIPLE LIMITATIONS of cupola melting with the usual acid lining is the sulphur increase due to absorption from the coke. This inevitable sulphur absorption places a limitation on remelting of scrap and less expensive materials and frequently necessitates ladle desulphurization to meet current specifications. Phosphorus is another element that places quality and economic limitations on both iron and steel. The conventional cupola neither increases nor decreases phosphorus content. Any means of removing phosphorus would be welcomed particularly in high phosphorus iron areas.

The recently developed nodular graphite iron has placed further importance on sulphur and phosphorus contents. Although not absolutely essential, better alloy recovery and better control results if the sulphur is low originally and the costly alloy is not consumed in desulphurizing.

Likewise the ductility advantages are realized more fully with low phosphorus contents. Irons chemically suitable for nodular treatment currently require either electric furnace melting, large scale ladle desulphurization previous to alloy addition, or excessive and unpredictable loss of the special alloy. Melting cost plus the alloy cost will keep nodular irons at a competitive disadvantage unless a less expensive, more direct method of obtaining the molten metal can be developed. Such a need stimulated this research on the basic cupola with the hope that a method might be

developed for melting suitable iron in the cupola, either directly in one operation or in a duplex combination.

Theoretically, any appreciable removal of either sulphur or phosphorus within the cupola would require a highly basic slag that could not be maintained in the conventional acid lining. In steel making, phosphorus is removed by a basic oxidizing slag and sulphur reduced by a basic reducing slag. Both operations require a large volume of slag in contact with a large bath area over a considerable period of time. It was the object of this study to investigate the possibility of removing phosphorus and sulphur in a cupola lined with basic refractories that would permit a highly basic slag.

No published reports have been found describing any basic cupola experimentation in this country. In Germany in 1934, Heiken¹ reported in not very complete detail some basic cupola heats that had been melted for duplex steelmaking. In comparison with acid heats the basic melts experienced lower sulphur, lower phosphorus, and higher carbon content, but a tremendous silicon loss, poor refractory life, and indications of poor control.

Prompted by the raw material emergencies at the beginning of the war, a British Committee was formed to investigate basic cupola operation. Much lower sulphurs were reported by Renshaw² and the committee in their 1944 report.³ In a recent article sulphurs as low as 0.044 per cent were reported by Renshaw and Sargood⁷ using a 12½ per cent flux. Some of the observations of Heiken were not confirmed and phosphorus was not removed by the same treatment. Small amounts of phosphorus were removed on a few special heats described in the second report in 1946.⁴

23-in. Cupola Used

Our experimentation was conducted along three lines. After a few experimental heats showed some promise, a 23-in. production cupola was lined with basic brick to determine the practicality of basic operation on an all-day production basis. Production experience is described in the first section. The cupola was operated successfully for full 9-hr shifts with good refractory life, and consistent chemical control.

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Numerous short heats were run on a 23-in. experimental cupola to investigate the potentialities of various fluxes and melting conditions. P and S would be expected to require opposite conditions for their optimum removal. Consequently experimentation was divided between these two objectives. The second part of this report describes results on 21 heats accomplishing some phosphorus removal. By several means sufficient phosphorus has been removed to lower high phosphorus iron to a medium-phosphorus iron to a malleable classification, etc.

The third part of the report includes over 70 short experimental heats directed toward a study of sulphur removal. The chemical differences between basic and acid linings with the same flux and charge were determined. Special flux combinations and strong reducing agents were found capable of removing large quantities of sulphur with the slag. Desulphurization efficiency was sufficient to produce low sulphur irons from very high sulphur coke and metal charges and some of the lowest sulphurs reported were obtained from raw materials of average quality.

The cupolas used are described in Table 1 below.

TABLE 1-DESCRIPTION OF CUPOLAS

| | Production | Experi | nental |
|----------------------------|---------------------------------|-----------------------------|----------------------------|
| | Cupola | "O" | "S" |
| 1 | (All heats in Tables 2 and 3 | (Experime 3) (Under 500) | ental heats) (Over 500) |
| Inside diameter, in. | 23 | 23 | 23 |
| Shell diameter, in. | 41 | 33 | 33 |
| Cupola area, sq in. | 415 | 415 | 415 |
| Number of tuyeres | 4 | 2 | 4 |
| Total tuyere area, sq in. | 72 | 32 | 32 |
| Tuvere ratio | 0.17 | .08 | .08 |
| Tuyere to bottom, in. | 20 | 12 | 12 |
| Tuyere to charge door | 10 ft, 0 in | . 7 ft, 4 in. | 7 ft, 4 in. |
| Ratio stack/diameter | 5.2 | 3.85 | 3.85 |
| Weight metal charges, lb | 155 | 100 | 100 |
| Type blower | Centrifuga | l Centrifugal | Positive |
| ** | | D | isplacement |
| Volume range, cfm | - I | Est. 500-700 | 650-900 |
| Pressure range, oz | 5-7 | 6-9 | 5-10 |
| Capacity of forehearth, ll | 500 | 300 | 300 |

The basic cupolas were lined with dead burned magnesite brick laid with magnesite mortar. Except where indicated the lining was patched with the same material. On all heats several metal samples were taken at intervals for chemical analysis. From known charge compositions and average final analyses, chemical changes within the cupola were determined and compared. Temperature measurements were made at regular intervals with a disappearing filament type, optical pyrometer which was standardized at intervals with a thermocouple. Some measurements of slag fluidity were made with a Herty type viscosimeter with a $\frac{3}{8}$ -in. channel. Representative slag samples were analyzed

chemically and spectrographically to aid in interpreting chemical changes in melting and desulphurization efficiency. Refractory consumption was determined from measurements made of the inside lining before and after each heat and the quantity of refractory used to patch to criginal dimensions.

Production Test of Basic Lining

One of two identical 23-in. cupolas (Table 1) was lined with regular 23-in. x 41-in. clay blocks for the full 9 ft. The other cupola was lined with magnesite brick for 6 ft (4½ ft above the tuyeres), using 9-in. keys for part of the lining and a double row of 4½-in. arch brick for a part. A regular sand bottom was used on these production heats.

After air drying over a week-end the acid and basic cupolas were operated on alternate days, melting an iron of 3.00 per cent silicon and 2.60 per cent carbon at a deliberately retarded rate of one ton per hour. Regular 155-lb charges consisting of 56 per cent small steel and 39 per cent returns were melted with front slagging and continuous tap. Additional silicon as briquets and manganese as spiegel were added to make up a theoretical charge of 3.80 per cent Si, 1.05 per cent Mn, 0.070 per cent S, and 1.23 per cent carbon. The charges were melted with 18 per cent coke and 4½ per cent dolomite or limestone. Bed height was 55 in. The metal was collected in a forehearth of 500-lb capacity where it was desulphurized with soda ash and dispensed in hand ladle quantities.

Because of lower silicon and higher than desired carbon on the first day of basic operation, it was found necessary to raise the steel from 56 to 60 per cent, cut the coke from 18 to 15 per cent, and raise the charged silicon from 3.80 to 4.45 per cent for the basic charge. This adjustment resulted in essentially the same final analysis for production purposes.

The basic-lined cupola was operated for ten 9-hr days with no major production delays or chemical irregularities. The regular acid plastic used in the rammed breast would last only a few hours. A basic plastic or clay plastic containing graphite was found to be sufficiently resistant to the basic slag for the breast. Basic refractories had to be used in the trough behind the slag knife. The poor plasticity of the basic mortar was a handicap until 5 to 10 per cent bentonite additions were made. Because of its narrower solidification range, the basic slag seems more temperature sensitive and chills more abruptly. Although sufficiently fluid at high temperature the basic slag is inclined to crust over and require more attention to keep it flowing freely down the trough.

Table 2 includes all data for each day of basic operation and Table 3 each day of acid operation. The averages are summarized below:

COMPARISON OF BASIC AND ACID CUPOLA IN A PRODUCTION OPERATION

| | | | | | Carbon | | | | | | | | 9-in. Equiv. | Aver. |
|-------|------------|-----------|------------|------------|-------------|-------------------|-------|----------|-----------|------|-----|-----|-----------------|-------|
| | % Steel | % Coke | Si Loss | Mn Loss | Pick- Up | Sulphur Stream | P | Lin 1 | ing Attac | 3 3 | 4 | 5 | to Patch | Temp. |
| Basic | 59.3 | 15.8 | 31.4 | 16.7 | 1.46 | 0.087 | 0.087 | 7.3 | 7.5 | 6.5 | 4.7 | 2.8 | 67 | 2765 |
| Acid | 56.0 | 18.0 | 22.6 | 27.3 | 1.37 | 0.117 | 0.093 | 14.3 | 13.4 | 11.5 | 9.2 | 6.8 | 205 | 2773 |

TABLE 2-BASIC LINING-23-IN. PRODUCTION CUPOLA

| | % | % | | | | Silico | n,* % | Carb | on, % | Mr | 1, % | Sulphur,2 | % Patch ⁸ | Avg.4 |
|--------------|-------|---------|-----|-------------------|-------|--------|----------|-------|--------|-------|------|-----------|----------------------|----------|
| Date | Steel | Coke | | Flux ¹ | P* | Final | Loss, 70 | Final | Pickup | Final | Loss | Final | (9 in Equiv.) | Temp., 1 |
| 12-1 | 56 | 18 | 4.5 | Dolomite | 0.10 | 2.56 | 32.6 | 2.73 | 1.50 | 0.84 | 20.0 | 0.095 | 36 | 2768 |
| 12-3 | 56 | 151/2 | 3.2 | Dol 1 Spar | 0.10 | 3.16 | 27.4 | 2.55 | 1.22 | 0.89 | 15.2 | 0.086 | 38 | 2774 |
| 12-6 | 60 | 15 | 2.7 | Dol 1 Spar | 0.07 | 3.10 | 30.4 | 2.65 | 1.57 | 0.85 | 19.0 | 0.084 | 68 | 2773 |
| 12-8 | 60 | 15 | 2.7 | Dol 1 Spar | 0 08 | 3.05 | 31.4 | 2.60 | 1.52 | 0.87 | 17.0 | 0.086 | 78 | 2768 |
| 12-10 | 60 | 15 | 4.5 | Lst 1 Spar | 0.08 | 3.01 | 32.3 | 2.60 | 1.52 | 0.86 | 18.0 | 0.081 | 700# Mag. | 2771 |
| 12-14 | 60 | 15 | 4.5 | Lst 1 Pur | 0.10 | 2.94 | 33.8 | 2.46 | 1.38 | 0.82 | 21.8 | 0.090 | 97 | 2757 |
| 12-16 | 60 | 16 | 4.5 | Dol 1 Spar | 0.07 | 3.02 | 32.2 | 2.48 | 1.40 | 0.91 | 13.3 | 0.091 | 83 | 2741 |
| 12-20 | 60 | 161/2 | 4.5 | Dol 1 Spar | 0.10 | 3.09 | 30.4 | 2.59 | 1.51 | 0.89 | 15.2 | 0.086 | 800# Dol. | 2766 |
| 12-22 | 2 br | s and I | rop | | | | | | | | | | None | |
| 12-28 | 60 | 161/2 | 4.5 | Lst 1 Spar | 0.09 | 3.02 | 32.2 | 2.63 | 1.55 | 0.93 | 11.4 | 0.089 | 150 | 2742 |
| Avg. | | | | | | | | | | | | | | |
| Basic | 59.3 | 15.8 | 4.8 | Tot. | 0.087 | 2.99 | 31.4 | 2.58 | 1.46 | 0.87 | 16.7 | 0.087 | 67 | 2764 |
| 11-30 | 56 | 20.6 | 4.5 | Dolomite | 0.10 | 3.04 | 20 | 2.79 | 1.56 | 0.80 | 23.8 | 0.105 | 183 | 2785 |
| 12-2 | 56 | 18 | 4.5 | Dolomite | 0.10 | 2.80 | 26.3 | 2.69 | 1.46 | 0.81 | 23.0 | 0.103 | 171 | 2781 |
| 12-4 | 56 | 18 | 4.5 | Dolomite | 0.06 | 2.94 | 22.5 | 2.62 | 1.39 | 0.82 | 21.9 | | 165 | 2790 |
| 12-7 | 56 | 18 | 4.5 | Dolomite | 0.08 | 2.83 | 25.5 | 2.52 | 1.29 | 0.75 | 28.6 | | 155 | 2772 |
| 12-9 | 56 | 18 | 4.5 | Dolomite | 0.09 | 2.96 | 22.0 | 2.47 | 1.24 | 0.70 | 33.3 | 0.124 | 158 | 2770 |
| 12-13 | 56 | 18 | 4.5 | Dolomite | 0.09 | 2.89 | 24 | 2.52 | 1.29 | 0.73 | 30.5 | 0.114 | 142 | 2780 |
| 12-15 | 56 | 18 | 4.5 | Dolomite | 0.08 | 2.79 | 26.5 | 2.52 | 1.29 | 0.75 | 28.6 | 0.117 | 218 | 2769 |
| 12-17 | 56 | 18 | 4.5 | Dolomite | 0.10 | 3.00 | 21.0 | 2.57 | 1.34 | 0.69 | 34.3 | 0.114 | 229 | 2777 |
| 12-21 | 56 | 18 | 4.5 | Dolomite | 0.09 | 3.04 | 20.0 | 2.58 | 1.35 | 0.77 | 25.7 | 0.116 | 248 | 2754 |
| 12-23 | 56 | 18 | | Limestone | 0.12 | 2.81 | 20.8 | 2.68 | 1.45 | 0.72 | 31.4 | 0.107 | 255 | 2764 |
| 12-29 | 56 | 18 | | Limestone | 0.09 | 3.00 | 21.0 | 2.67 | 1.44 | 0.83 | 21.0 | 0.127 | 266 | 2764 |
| 12-31 | 56 | 18 | | Limestone | 0.08 | 2.90 | 23.6 | 2.56 | 1.33 | 0.80 | 23.8 | 0.115 | 238 | 2777 |
| 1-4 | 56 | 18 | | Limestone | 0.12 | 3.00 | 21.0 | 2.60 | 1.37 | 0.73 | 30.4 | 0.132 | 250 | 2776 |
| Avg. Acid | 56 | 18 | 4.5 | | 0.093 | 2.92 | 22.6 | 2.60 | 1.37 | 0.76 | 27.3 | 0.117 | 205 | 2773 |

* Si, C, and S are average of hourly samples. P and Mn average of 3 and 5 samples.

1. Quantity of coke and flux based on percentage of metal charge.

2. Final sulphur average of hourly samples from cupola stream.

Brick required to patch to operating dimensions. Splits and parts converted to 9-in. equivalents.
 Temperature determinations made hourly on the stream with an optical pyrometer.

TABLE 4-COMPARISON OF SLAG CHEMISTRY-ACID AND BASIC PRODUCTION HEATS

| | | | Sulphur | | | 5 | lag Com | position, | % | | | | Basicit |
|-------|------|----------------|---------|------------------|--------------------------------|----------|---------|-----------|------|------------------|------------------|-------|---------|
| Date | Hour | Flux, % | Metal | SiO ₂ | Al ₂ O ₃ | FeO* | MnO | CaO | MgO | NaO ₂ | CaF ₂ | S | Ratio |
| | | | | | ACID (| CLAY) L | INING | | | | | | |
| 12-9 | 6th | 41/2 Dol | 0.124 | 48.5 | 12.8 | 4.8 | 4.1 | 18.2 | 10.4 | | | 0.105 | 0.59 |
| 12-15 | 6th | 82 89 | 0.117 | 46.6 | 14.7 | 3.6 | 3.9 | 20.3 | 10.2 | | | 0.115 | 0.65 |
| 12-29 | 3rd | 41/2 Lst | 0.142 | 46.3 | 12.2 | 3.3 | 3.9 | 31.0 | 1.6 | | | 0.185 | 0.68 |
| 12-29 | 6th | 22 19 | 0.130 | 48.7 | 10.1 | 2.6 | 2.7 | 32.5 | 1.6 | | | 0.205 | 0.70 |
| 12-29 | 8th | 31 11 | 0.123 | 46.2 | 9.8 | 1.6 | 2.9 | 37.3 | 1.0 | | | 0.210 | 0.83 |
| | | | | I | BASIC (MA | GNESITE) | LINING | | | | | | |
| 12-6 | 6th | 2.7 Dol 1 Spar | 0.084 | 33.4 | 10.4 | 4.5 | 2.9 | 25.8 | 18.0 | | 3.0 | 0.427 | 1.37 |
| 12-20 | 6th | 4.5 Dol 1 Spar | 0.086 | 31.6 | 11.4 | 4.4 | 3.4 | 26.0 | 16.3 | | 0.7 | 0.532 | 1.36 |
| 12-10 | 6th | 4.5 Lst 1 Spar | 0.081 | 30.7 | 13.3 | 4.6 | 3.2 | 33.9 | 8.7 | | 2.4 | 0.575 | 1.46 |
| 12-28 | 6th | 4.5 Lst 1 Spar | 0.089 | 33.4 | 13.3 | 1.5 | 2.7 | 34.3 | 12.7 | | 0.6 | 0.993 | 1.42 |
| 12-14 | 6th | 4.5 Lst 1 Pur. | 0.090 | 39.2 | 11.8 | 1.9 | 3.7 | 31.5 | 8.7 | 2.2 | | 0.603 | 1.08 |

* Charge details and metal chemistry essentially identical and given in Tables 2 and 3.

Charges of 56% to 60% steel melted with 15 to 18% coke.

* FrO is total of both FeO and Fe₂O₃ as FeO. Basicity ratio = $\frac{\text{CaO} + \text{MgO} + \text{Na}_2\text{O}}{\text{SiO}_2}$

The slightly greater loss of the acid element silicon and slightly lower loss of the basic element Mn in the basic cupola would be theoretically expected. A slightly higher carbon pick-up was experienced. The melting temperatures were comparable; the slight difference may be accounted for by the difference in coke. The most significant difference is the 0.030 per cent lower sulphur obtained as melted on the basic heats. Actually the sulphur as melted was low enough to not require the usual forehearth desulphurization. Phosphorus was only 0.006 per cent lower on the basic heats which is within the limits of analytical accuracy.

Essentially no P was removed under these conditions characterized by a relatively high percentage of coke and a low slag FeO of 1.5 to 4.5 per cent.

No appreciable difference between limestone and dolomite was apparent in these heats. The use of 1 per cent flurospar seemed to give improved slagging over the one day in which 4½ per cent dolomite was used with no spar. Substitution of 1 per cent purite for 1 per cent spar seemed equally satisfactory, but no particular advantage was evident.

The analyses of some typical slags from the acid and basic operation are shown in Table 4. The principal

difference is the 15 per cent higher SiO₂ in the acid slags and 6 to 12 per cent higher MgO in the basic slag because of the consumption of the respective refractories.

Whereas the sulphur content of the acid slags ran 0.105 per cent to 0.210 per cent the basic slags ranged from 0.427 per cent to 0.993 per cent. This difference accounts for the lower metal sulphur. Sulphur in the slag seemed to follow roughly the basicity ratio,* which was only 0.59 to 0.83 in the acid slags but 1.08 to 1.46 in the basic slags.

On the basis of the European reports we expected inferior service life and some refractory troubles from the basic brick. To the contrary, however, less burnout was experienced and refractory life was much su-

perior to the regular clay bricks used.

The better refractory life of the magnesite brick is indicated graphically in Fig. 1, showing a cross-section of both acid and basic linings after the first day of operation, as well as the average daily burn out for the period of the study. The superiority of the basic lining is indicated further in Tables 2 and 3. The average number of 9-in. equivalents required to patch after each day's operation was 205 for the acid lining and only 67 for the basic lining.

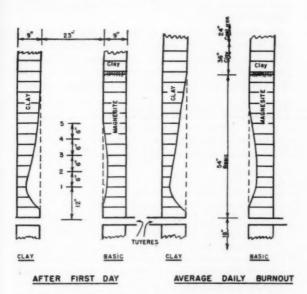


Fig. 1—Comparison of Refractory Burnout and Basic Production Cupolas.

One day the basic cupola was patched with a rammed mixture of magnesite plus 10 per cent bentonite tempered with water. The application was satisfactory and service life seemed to be nearly equal to magnesite brick as a patch. For this basic plastic, material cost was lower but labor cost was enough higher to make the total cost comparable.

One patch in the basic cupola was rammed from a

mixture of stabilized dolomite imported from England. This was tempered with a 1:5 silicate solution. After 2 hr melting on this patch, blower trouble caused operations to be discontinued for the day. Inside measurements indicated that within this short period of operation the lining had been consumed almost to the dimensions before the patch. There is considerable evidence that consumption is generally greater the first few hours but not to this extent. It seems safe to conclude that the dolomite patch was inferior to the magnesite. The cupola was not patched for the next run. Lining attack for the equivalent of a second days operation was only 2 in. on each side, and still not as bad as the usual acid lining after one day.

The basic patch seems inferior to the original lining. The lower plasticity of the magnesite mortar and the greater spalling tendency of the brick make thin patches inclined to fall out before they burn out. A deeper patch that permits better anchoring of the brick seems more effective. It appears that a better job of patching could be done on a deeper burn-out and on a larger diameter cupola. From all indications the basic lining could be operated two or three times as long as an acid lining before requiring patching. In the upper part of the stack the basic brick proved considerably superior to clay brick in abrasion resistance.

Because of the unusual operation this cupola is not at all typical. Refractory costs are several times higher than on larger cupolas operated at a more efficient rate and with less steel on the mix. The original lining cost, material plus labor, for the 23 in. x 41 in. x 9-ft lining calculated for clay blocks was \$138.00, for a complete lining of magnesite brick \$553.00, and for a two-thirds basic lining as used, \$415.

Although basic brick required only one-third the patching, the material cost of five to seven times that of clay leaves the patching cost nearly twice that of clay brick. The cost of the average daily patch was \$24.15 for clay brick and \$46.00 for the magnesite brick. Table 5 below summarizes the analysis of refractory cost on this particular operation on which no advantage was taken of the improved refractory life. The cost comparison is considered the maximum difference.

Table 5—Comparison of Refractory Costs 23-in. Cupola*

| | Clay | 36 Magnesite |
|---|----------|--------------|
| Total cost of original lining | \$138.00 | \$415.00 |
| Cost per ton (495 tons/5 mon) (Assume same life) | .28 | .84 |
| Cost of daily patch | 25.15 | 46.08 |
| Cost of patch per ton | 2.68 | 5.12 |
| Total refractory cost per ton | 2.96 | 5.96 |

This difference in cost could be reduced by properly utilizing the improved life through less frequent patching and by utilizing more fully the chemical possibilities of a basic slag.

^{*} Basicity ratio considered as (CaO+MgO+No₂O) until fur-SiO₂

ther research can determine more accurately the quantitative relationships and the effect of other constituents.

Cupola described in Table 1, charges and metal chemistry in Tables 2 and 3.

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No doubt with more experience basic refractory consumption could be improved further by reducing the basic portion of the lining, and/or substituting some cheaper basic materials. However, no evidence was obtained that indicated the cost of the basic refractories might be immediately justified in the melting of ordinary gray iron under good raw material conditions, and under current specifications. However, the cost might be justified for many special irons and some unusual raw material situations. No attempt was made in these heats to utilize the full potentialities of the basic process. These were explored in the experimental heats to follow.

Attempts to Remove Phosphorus-Basic Experimental Heats

No experimentation has been reported on any attempt to remove phosphorus in the cupola at high levels of phosphorus content. The investigations reported were on steel charges below 0.10 per cent P for duplex steel making. Heiken reported 0.059 per cent P from a basic cupola on a charge yielding 0.091 per cent in an acid cupola, for a P drop of 0.032 per cent.¹ The British committee reported a P of 0.056 per cent from a charge of 0.070 per cent for a drop of 0.014 per cent.⁴

Phosphorus eliminations of these amounts have some value in steelmaking but are of insufficient magnitude for much value in cast iron production. However, if several times this amount of phosphorus could be removed, the practicality of high phosphorus irons produced in some sections of our country could be greatly extended. Irons from Southern areas containing approximately 0.75 per cent P are well suited for some applications but are at a serious disadvantage in castings of varying section size requiring pressure tightness. Many foundries are forced to import more expensive low phosphorus irons for a part of their production. The need for low phosphorus content for nodular graphite iron places further value on any means of phosphorus elimination.

From a theoretical analysis, removal of phosphorus would require oxidizing conditions to convert the phosphorus to its oxide and a slag with excess basicity to retain the phosphorus oxide in the slag as a basic phosphate. Silicon is oxidized at essentially the same temperature as P and theoretically must be removed before phosphorus oxidation proceeds in steel making. The necessary silicon level will have to be determined for the conditions in question.

A number of short experimental heats were run in the 23-in. experimental cupolas described in Table 1. On each heat fifteen 100-lb charges were melted, collected in a 300-lb forehearth ladle and shanked in 80 to 50-lb quantities. Lower bed heights of 35 to 45 in. were used to increase oxidation. Granular magnesite was used for the cupola bottom instead of sand to insure maximum slag basicity. The chemical results are summarized in Table 6.

On Heat 410 a charge of local pig iron of 0.93 per cent Si and 0.74 per cent P was melted with a relatively low percentage of coke to increase oxidation, 10 per cent coke A (coke properties described in Table 8). The flux used was 10 per cent limestone and 1 per

TABLE 6-BASIC EXPERIMENTAL HEATS ATTEMPTING PHOSPHORUS REMOVAL

| | | 1. | | | | | | | | | | | 1 | Phos, | | | |
|-------|---------------------------|------|-----------------|--------|--------|-------|-------|-------|-------|-------|---------|-------|--------|-------|------|--------|------------|
| Heat | * | % | | Silie | on, % | Mn | , % (| Carbo | on, % | Sulph | nur, % | Phos | 5, % 0 | % Re- | Ten | np. F | Slag |
| No. | Flux, % | Coke | Charge | Chg | Final | Chg | Final | Chg | Final | Chg | Final (| Chg F | inalı | novec | Avg | . High | Fluid |
| | | | Con | TINUO | US TAI | P—FRO | DNT | SLAG | GING | | | | | | | | |
| 410 | 10 Lst. 1 Spar | 10 | A 100% Pig | 0.93 | 0.46 | 0.39 | 0.25 | 4.30 | 3.48 | 0.071 | 0.069 | 0.74 | 0.55 | 0.19 | 2460 | 2490 | 4 in. |
| 411 | 10 Lst. 1 Spar | 89 | A Re 410 | 0.46 | 0.16 | 0.25 | 0.15 | 3.48 | 3.47 | 0.069 | 0.075 | 0.55 | 0.41 | 0.14 | 2500 | 2540 | |
| 412 | 10 Lst. 1 Spar | 22 | A Re 411 | 0.16 | 0.09 | 0.15 | 0.09 | 3.47 | 5.09 | 0.075 | 0.087 | 0.41 | 0.20 | 0.21 | 2530 | 2550 | |
| 400 | 10 Lst. 1 Spar 3 Fe Ore | 22 | A 100% Pig | 0.93 | 0.48 | 0.39 | 0.22 | 4.30 | 3.64 | 0.071 | 0.085 | 0.74 | 0.65 | 0.09 | 2565 | 2580 | Foamy |
| 401 | 10 Lst. 1 Sp. 3 Fe Ore | 27. | A Re 400 | 0.48 | 0.17 | 0.22 | 0.13 | 3.64 | 3.55 | 0.085 | 0.091 | 0.65 | 0.33 | 0.32 | 2520 | 2530 | Foamy |
| 402 | 10 Lst. 1 Sp. 3 Fe Ore | 99 | A Re 401 | 0.17 | 0.12 | 0.13 | 0.05 | 3.55 | 3.24 | 0.091 | 0.097 | 0.33 | 0.14 | 0.19 | 2520 | 2540 | Foamy |
| 420 | 10 Lst. 1 Sp 3 Mn Ore | 14 | A 100% Pig | 0.75 | 0.23 | 0.24 | 0.39 | 4.13 | 3.79 | 0.037 | 0.036 | 0.79 | 0.58 | 0.21 | 2590 | 2640 | Fluid |
| 421 | 10 Lst. 1 Sp 3 Mn Ore | 14 | A Re 420 | 0.23 | 0.10 | 0.39 | 0.43 | 3.79 | 3.62 | 0.036 | 0.046 | 0.58 | 0.28 | 0.30 | 2540 | 2610 | Fluid |
| 422 | 8 Lst. 3 Sp 3 Mn Ore | 14 | A Re 421 | 0.10 | 0.13 | 0.43 | 0.38 | 3.62 | 3.65 | 0.046 | 0.54 | 0.38 | 0.12 | 0.16 | 2530 | 2550 | Very fluid |
| 430 | 8 Lst 4 Purite | 12 | A 70 Pig 30 Stl | 0.56 | 0.10 | 0.31 | 0.12 | 2.95 | 2.80 | 0.041 | 0.081 | 0.57 | 0.30 | 0.27 | 2525 | 2550 | Foamy |
| 431 . | 8 Lst 4 Spar | 15 | A 70 Pig 30 Stl | 0.56 | 0.23 | 0.31 | 0.26 | 2.95 | 3.65 | 0.041 | 0.040 | 0.57 | 0.42 | 0.15 | 2600 | 2630 | Fluid |
| 132 | 6 Lst 4 Pur 2 Sp | 15 | A 70 Pig 30 Stl | 0.56 | 0.17 | 0.31 | 0.18 | 2.95 | 3.04 | 0.041 | 0.083 | 0.57 | 0.38 | 0.19 | 2575 | 2660 | 5 in. |
| 523 | 8 Lst 3 Sp 3 Fe Ore | 15 | A 70 Pig 30 Stl | 0.56 | 0.31 | 0.31 | 0.21 | 2.95 | 3.91 | 0.041 | 0.068 | 0.57 | 0.42 | 0.15 | 2700 | 2740 | 7 in. |
| 524 | 8 Lst 3 Sp 3 Mn Ore | 15 | A 70 Pig 30 Stl | 0.56 | 0.34 | 0.31 | 0.41 | 2.95 | 3.70 | 0.041 | 0.047 | 0.57 | 0.45 | 0.12 | 2635 | 2650 | 8 in. |
| 525 | 6 Lst 4 Pur 2 Sp | 12 | A 70 Pig 30 Stl | 0.56 | 0.20 | 0.31 | 0.21 | 2.95 | 3.21 | 0.041 | 0.084 | 0.57 | 0.38 | 0.19 | 2520 | 2570 | 7 in. |
| 526 | 8 Lst 3 Sp 3 Mn Ore | 15 | A 50 Pig 50 Stl | 0.42 | 0.14 | 0.31 | 0.42 | 2.20 | 3.57 | 0.043 | 0.061 | 0.42 | 0.32 | 0.10 | 2630 | 2660 | 7 in. |
| | | | INTE | RMITTE | NT TA | AР—В. | ACK | SLAGO | GING | | | | | | | | |
| 527 | 6 Lst 4 Pur 2 Sp | 12 | A 30 Stl 70 Pig | 0.56 | 0.15 | 0.31 | 0.22 | 2.45 | 3.25 | 0.041 | 0.070 | 0.57 | 0.38 | 0.19 | 2590 | 2620 | Fluid |
| 528 | 8 Lst 3 Sp 3 Mn Ore | 12 | A 50 Stl 50 Pig | 0.42 | 0.19 | 0.31 | 0.30 | 2.20 | 3.34 | 0.043 | 0.062 | 0.42 | 0.27 | 0.15 | 2625 | 2630 | Very fluid |
| 529 | 8 Lst 3 Sp 3 Fe Ore | | A 50 Stl 50 Pig | 0.42 | 0.11 | 0.31 | 0.10 | 2.20 | 2.70 | 0.043 | 0.081 | 0.42 | 0.24 | 0.18 | 2600 | 2630 | Foamy |
| 530 | 6 Lst 4 Pur 2 Sp 3 Mn Ore | 12 | A 100 Pig | 0.75 | 0.37 | 0.24 | 0.27 | 4.13 | 3.65 | 0.037 | 0.052 | 0.79 | 0.49 | 0.30 | 2500 | 2570 | |
| 531 | 5 Lst 4 Pur 3 Sp 3 Mn Ore | | A Re 530 | 0.37 | 0.13 | 0.27 | 0.22 | 3.65 | 3.19 | 0.052 | 0.074 | 0.49 | 0.23 | 0.24 | 2530 | 2550 | |

Flux expressed as per cent of metal charge. Lst = limestone, Dol = dolomite, Purite = fused sodium earbonate, Spar = Fluorspar (calcium floride)

Coke expressed as per cent of metal charge. Type indicated by letter. Properties in Table 8 further in paper. All analyses represent average of 3 to 7 samples. Temperature determined by optical pyrometer standardized at intervals with a thermocouple. Average temperature is average of 10 to 15 readings. Maximum temperature is the average of three highest adjacent readings. Slag fluidity measurements made in Herty viscosimeter with 3/6-in. channel expressed as inches of run. Because of poor reproducibility each figure is rounded average of 3 or more determinations.

TABLE 7-SLAG COMPOSITION-BASIC DEPHOSPHORIZING HEATS

| Heat | Ladle | | Metal | Phos. | | | | | Slag (| omposi | tion, % | | | |
|------|-------|------------------------|-------|-------|------------------|--------------------------------|---------------------------------|------|--------|--------|---------|------------------|------|-------------------------------|
| No. | No. | Flux, % | Chg | Final | SiO ₂ | Al ₂ O ₃ | $\mathbf{Fe}_{2}\mathbf{O}_{3}$ | FeO | MnO | CaO | MgO | CaF ₂ | S | P ₂ O ₅ |
| 400 | 1 | 10 Lst, 1 Sp, 3 Fe Ore | 0.74 | 0.65 | 28.4 | 9.3 | 7.6 | 19.5 | 2.0 | 21.6 | | 1.8 | 0.02 | 2.5 |
| 400 | 10 | 10 Lst, 1 Sp, 3 Fe Ore | 0.74 | 0.65 | 16.0 | 3.5 | 9.1 | 32.2 | 1.5 | 20.4 | 14.4 | 2.5 | 0.14 | 2.9 |
| 401 | .10 | 10 Lst, 1 Sp, 3 Fe Ore | 0.65 | 0.33 | 20.3 | 6.2 | 9.4 | 23.0 | 1.3 | 28.3 | 9.2 | 2.0 | 0.08 | 2.9 |
| 410 | 10 | 10 Lst, 1 Sp | 0.74 | 0.55 | 31.3 | 7.1 | 4.2 | 4.6 | 2.7 | 39.7 | 5.2 | 2.4 | 0.11 | 3.5 |
| 412 | 3 | 10 Lst, 1 Sp | 0.41 | 0.20 | 14.3 | 4.4 | 18.6 | 19.8 | 0.9 | 35.5 | 11.0 | 1.0 | 0.11 | 1.7 |

TABLE 8-PROPERTIES OF TWO COKES USED

| Designation | Coke "A" | Coke "D" |
|--------------------------------|-----------------|----------------|
| General Type | (Fdry. Quality) | (High Sulphur) |
| Sulphur, % | 0.65- 0.85 | 2.0 |
| Volatile, % | 0.60- 1.00 | 5.0 |
| Ash, % | 9.0 - 10.5 | 14.0 |
| True Sp. Gr. | 1.85- 1.95 | 1.97 |
| Apparent Sp. Gr. | 0.95 - 1.05 | 0.98 |
| Porosity, 17 | 48-51 | 50.5 |
| Shatter Test, 2 in. Plus, % | 81-86 | 83.5 |
| Ash Co | mposition, % | |
| SiO ₂ | 52 | 40 |
| Fe ₂ O ₂ | 10 | 40 |
| Al_2O_3 | 30 | 20 |
| CaO | 2.3 | 10 |
| MgO | 1.3 | - |
| Sulphates | _ | 5 , |

cent spar, doubled on the bed charge. Silicon was oxidized from 0.93 per cent to 0.46 per cent and Mn from 0.39 per cent to 0.25 per cent. Carbon dropped from 4.30 per cent to 3.48 per cent. Sulphur remained essentially the same. Phosphorus was oxidized from 0.74 per cent to 0.55 per cent for a removal of 0.19 per cent P in spite of the 0.46 per cent Si left in the metal.

On Heat 411 the metal pigged from the previous heat was remelted under the same condition of coke and flux. This made possible a lower Si charge and a chance to observe the effect of metal Si. Silicon and manganese were oxidized further and sulphur was increased. Phosphorus was oxidized from 0.55 per cent to 0.41 per cent for a removal of 0.14 per cent. A ladle addition of soda ash removed another 0.11 per cent phosphorus, producing a 0.28 P on one ladle. (Not included in average.) Another remelt followed the same trend with P dropping from 0.41 per cent to 0.20 per cent for a removal of 0.21 per cent in the cupola. A ladle addition of soda ash removed another 0.08 per cent P along with 0.030 per cent sulphur.

On the three heats phosphorus elimination in the cupola averaged 0.18 per cent. Because of the low coke ratio and excess flux the melting temperatures of 2450 to 2550 F were rather low for castings. Likewise the depleted Si and Mn contents require rebuilding for gray iron castings. On one or two ladles of each heat additions of approximately 2.30 per cent Si and 0.70 per cent Mn were made and test bars cast. With cold alloy additions recoveries of about 60 to 80 per cent were realized and the iron was restored to a reasonable gray iron analysis with proper chill depth and physical properties. On some heats premelting the ferroalloys in an auxiliary induction furnace resulted in improved recovery and elimination of temperature loss.

The next three heats (Heats 400 to 402), are con-

secutive remelts of the same 0.93 per cent Si, 0.74 per cent P pig iron with the same flux. Coke was increased to 14 per cent, and 3 per cent iron ore was added with the flux to supply the oxidizing influence. Losses of Si, Mn, and C, and increases in sulphur were comparable to the previous series. Phosphorus elimination was 0.09 per cent on the first, 0.32 per cent on the second, and 0.19 per cent on the third remelt. The average of 0.20 per cent P removal per heat for the three remelts was only slightly better than the 0.18 per cent averaged by the previous series with no ore and less coke. Temperature was improved slightly.

On the next series of three heats (Heats 420 to 422) 3 per cent manganese ore was used instead of iron ore. A pig iron with a slightly lower Si content of 0.75 per cent was used. Silicon was oxidized with equal readiness by the Mn ore. However, no Mn was lost, actually a slight increase resulted from the Mn ore. Sulphur absorption was decreased considerably, carbon dropped less, temperature was generally better, and slag was more fluid. The best phosphorus elimination of any series was experienced. Phosphorus was eliminated from 0.79 per cent to 0.58 per cent, from 0.58 per cent to 0.28 per cent, and from 0.28 per cent to 0.12 per cent, on the three consecutive remelts for an average of 0.22 P per heat.

On the next few heats 30 per cent steel was used with 70 per cent pig with the hope that Si and P would be diluted and some oxidizing influence exerted by the steel. On Heat 430 such a 30 per cent steel charge was melted with 12 per cent coke "A" and a flux of 8 per cent limestone and 4 per cent purite (fused sodium carbonate). Phosphorus dropped from 0.57 per cent to 0.30 per cent but the low carbon of 2.80 per cent and the low average temperature of 2525 F made this heat difficult to cast. Coke was raised to 15 per cent for the next four heats to obtain higher temperatures and carbon. However, dephosphorization effectiveness was sacrificed by the less oxidizing coke ratio.

On Heat 431 with a flux of 8 per cent limestone and 4 per cent fluorspar, 0.15 per cent P was removed, from 0.57 per cent to 0.42 per cent. The carbon averaged 3.65 per cent and the temperature 2600 F. On Heat 432 with 6 per cent limestone, 4 per cent purite, and 2 per cent spar, P elimination was 0.19 per cent, from 0.57 per cent to 0.38 per cent, and carbon averaged 3.04 per cent. On Heat 523 with 8 per cent limestone, 3 per cent spar, and 3 per cent iron ore, P elimination was 0.15 per cent. Fluidity of the slag was improved over the 400 to 402 series by the increase in spar. The slag flowed freely down the continuous slagging trough and fluidity averaged 7 in. On Heat 524 manganese ore was substituted for the iron ore in a

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heat otherwise identical with Heat 523. Phosphorus eliminated was 0.12 per cent. On Heat 525 the most effective flux of 6 per cent limestone, 4 per cent purite and 2 per cent spar (used on Heat 432), was repeated with the coke decreased to 12 per cent. Phosphorus elimination was the same with 0.19 per cent removed.

Because of the higher carbons resulting from the manganese ore it was possible for the proportion of steel to be increased to 50 per cent on Heat 526. A flux of 8 per cent limestone, 3 per cent spar, and 3 per cent Mn ore identical to Heat 524 was used. Carbon was still reasonably high at 3.57 per cent. Although the Mn ore permitted the use of a charge 0.15 per cent lower in P, the actual P removal was only 0.10 per cent.

The last five heats in Table 6 were operated with intermittent tapping and back slagging. All other heats in this investigation were continuously tapped with front slagging. These were intended to check the possibility of improved dephosphorization from longer slag-metal contact as well as the possibility of maintaining higher carbon contents. Heat 527 was identical with Heat 525 except for the intermittent tapping. The flux was 6 per cent limestone, 4 per cent purite, and 2 per cent spar. The phosphorus elimination of 0.19 per cent was identical with the continuously tapped heat. The other elements were as nearly identical as could be reproduced. On Heat 528 a 50 per cent steel charge was melted with 12 per cent coke "A." Flux was 8 per cent limestone, 3 per cent spar, and 3 per cent Mn ore. Phosphorus elimination was 0.15 per cent, from 0.42 per cent to 0.27 per cent, and carbon averaged 3.34 per cent. On Heat 529 everything was identical with the previous heat except iron ore was used. Phosphorus elimination was 0.18 per cent, but carbon averaged only 2.79 per cent.

On Heat 530 a straight pig iron charge was melted with 12 per cent coke. A combination of limestone, purite, Mn ore, and spar was used totaling 15 per cent flux, hoping the Mn ore and sodium carbonate would counteract and supplement each other. Phosphorus was oxidized from 0.79 per cent to 0.49 per cent. On Heat 531 a remelt of this heat lowered the 0.49 per cent P to 0.24 per cent. Average temperatures were only 2500 F and 2530 F because of the quantity of flux

but dephosphorization was improved.

From this limited number of heats it appears that phosphorus elimination is not improved significantly by the larger slag contact from intermittent tapping. This suggests that the factors preventing greater phosphorus removal are chiefly chemical rather than mechanical. Larger quantities of slag should remove more phosphorus in the cupola but melting temperature is decreased by increased quantities of flux. Increasing the coke to compensate for the temperature loss decreases the degree of oxidation and the effectiveness of P removal. Increasing the overall thermal capacity of the cupola by external devices like oxygen enrichment or preheated blast might increase the overall dephosphorizing capacity.

Among the various fluxes, purite was generally the most effective suggesting that sodium oxide is more powerful than calcium oxide in phosphorus elimination. Likewise the sodium carbonate proved very oxidizing as indicated by greater losses of Si and Mn. The principal disadvantage was the much lower resulting carbon.

From a practical standpoint manganese ore with limestone and spar was one of the most effective combinations. The highest carbon resulted which would permit increased proportions of low P steel scrap. Lower sulphurs and better Mn recoveries resulted, as well as better slagging. Using Mn ore with purite counteracted some of the low carbon tendencies of the

purite.

As long as temperature permits, further dephosphorization can be accomplished in a forehearth or holding ladle, by additions of dephosphorizing reagents. Sodium carbonate has proved particularly effective when added to the metal from the cupola already low in silicon. As much as 0.10 to 0.15 per cent additional phosphorus has been removed, along with 0.020 to 0.030 per cent sulphur by 1 per cent addition of soda ash to small shank ladles. Better results should be experienced in a large forehearth where more favorable time and degree of contact could be maintained.

Refractory life was unexpectedly good on these oxidizing heats. In a conventional acid-lined cupola a heat like Heat 529 with 50 per cent rusty steel in the charge and 3 per cent iron ore as part of the flux, would literally dissolve the refractories in the melting zone. However, with a lining of magnesite brick no measurable attack resulted from two such heats. Only after three or four heats was as much as 1 in. consumed on each side and only after 10 or 12 heats was patching required (about 2 in. per side). This resistance to iron oxide seems to be the explanation for the improved refractory life of magnesite refractories in the cupola.

Slag analyses from a few of the oxidized heats are shown in Table 7. P_2O_5 contents of these slags ran from 1.7 per cent to 3.5 per cent. The most significant constituent was the iron oxide. Total FeO ran gener-

ally from 25 per cent to 40 per cent.

This oxidation treatment to remove phosphorus would be difficult to apply in most gray iron foundries. Practical application seems more likely in the duplex melting of malleable iron. The low silicon and low carbon needed for malleable compositions would suit the oxidizing conditions. The usual air furnace or electric furnace following the cupola would permit the necessary addition of silicon, increase in temperature, and adjustment of chemistry.

Greater dephosphorization could be accomplished if castability could be disregarded and the metal pigged for remelting. With lower melting temperatures permissible, it should be easy for increased quantities of flux and subsequent forehearth additions to remove 0.40 to 0.50 per cent P from 0.75 per cent P

iron.

Experimental Heats Attempting Desulphurization

The next part of the discussion describes a group of short experimental heats on which various fluxes, types of coke, and metal charges were used to explore the potential desulphurizing possibilities of the basic-lined cupola under continuous melting conditions.

Since the source of sulphur absorbed in cupola melt-

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ing is the coke, the sulphur content and properties of the coke are important. MacKenzie⁶ has shown that upon repeated remelting of the same charge the sulphur content of the metal increases with each remelting until an equilibrium level is reached. With coke of average quality this equilibrium level is higher than usual specifications. Carbon also follows a similar pattern. These equilibrium levels of both sulphur and carbon depend upon the properties of the coke and serve as a useful indication of coke quality. Of course, the distribution of sulphur between the metal and slag is sensitive to the Mn content of the metal and the slag composition. Both of these must be controlled or recognized.

The two cokes used in these experiments are described in Table 8. Coke "A" is an average foundry grade coke with 0.75 per cent S. Coke "D" contains 2.0 per cent sulphur which makes it normally unsuitable for cupola use. This coke was set aside for experimentation of this type.

To evaluate this high-sulphur coke a series of remelts had been run in an acid-lined cupola. To compare acid and basic linings under such high-sulphur conditions, repeated remelts were run in the same cupola with a basic lining, using the same charge, flux, and melting practice. For the first heat a 70 per cent steel charge was melted with 15 per cent high-sulphur coke "D" and cast into pigs. The pigged metal was then repeatedly remelted with 121/2 per cent highsulphur coke, adding ferroalloys to maintain Si near 2.00 per cent and Mn near 0.70 per cent. On all heats the regular flux was 3 per cent dolomite and 1 per cent purite. For the first bed charge the flux was tripled and on the next two charges doubled to compensate for the effect of the high bed of high ash coke. Regular bed height was 50 in. (increased to 55 in. on the first high steel heat). Fifteen 100-lb charges were used on the first heat and the total weight reduced with each successive remelt due to metal loss and test

bars removed from the system.

The results of these remelts on high sulphur coke in both the acid and basic lining are graphed in Fig. 2. Upon melting the first charge of 70 per cent steel in the acid cupola the charge of 0.078 per cent sulphur jumped immediately to 0.219 per cent S. Repeated remelts remained at a level averaging 0.233 per cent sulphur. The effect of Mn on sulphur can be seen. The accidental low Mn of remelt No. 3 caused a sulphur 0.040 per cent higher than the average. On acid remelt No. 10, Mn was intentionally increased to 0.80 per cent, and as a result sulphur was dropped to 0.215 per cent. On the last heat Mn was intentionally

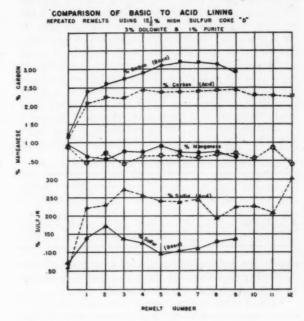


Fig. 2

TABLE 9-EXPERIMENTAL DESULPHURIZING HEATS-AVERAGE COKE-VARIOUS FLUXES

| Heat | | | | | Si | Mn | Carb | on, % | | Sulphur, | 76 | Tem | p., F | Slag |
|------|------------------|-------|-----|----------|-------|-------|------|-------|-------|----------|-------|------|-------|----------|
| No. | Flux, % | Coke, | % | Charge | Final | Final | Chg. | Final | Chg. | Final | Low | Avg. | Max. | Fluidity |
| 11 | 7 Dolomite | 121/2 | A | Fdy. Re. | 2.30 | 0.66 | 3.28 | 3.33 | 0.077 | 0.081 | 0.075 | 2550 | 2600 | Fluid |
| 12 | 7 Dolomite | 22 | 99 | Re. 11 | 2.30 | 0.68 | 3.33 | 3.32 | 0.081 | 0.080 | 0.075 | 2550 | 2570 | 99 |
| 13 | 2 Met. Lime | 11 | 9.0 | Re. 11 | 2.06 | 0.71 | 3.32 | 3.44 | 0.080 | 0.693 | 0.088 | 2600 | 2630 | ** |
| 1 | 3 Dol. 4 Purite | 93 | 93 | Fdy. Re. | 2.44 | 0.78 | 3.31 | 3.31 | 0.068 | 0.049 | 0.038 | 2560 | 2590 | 99 |
| 2 | 3 Dol. 4 Purite | 80 | 22 | Re. 1 | 2.10 | 0.75 | 3.31 | 3.37 | 0.049 | 0.059 | 0.050 | 2570 | 2600 | 99 |
| 3 | 3 Dol, 4 Purite | | 99 | Re. 2 | 2.01 | 0.68 | 3.37 | 3.35 | 0.059 | 0.057 | 0.054 | 2530 | 2580 | 99 |
| 18 | 4 Purite | 99 | 22 | Re. 17 | 1.86 | 0.76 | 3.88 | 3.73 | 0.054 | 0.066 | 0.058 | 2540 | 2560 | ** |
| 19 | 8 Purite | 19 | 9.5 | Re. 18 | 1.82 | 0.77 | 3.73 | 3.62 | 0.066 | 0.071 | 0.070 | 2560 | 2600 | ** |
| 20 | 2 Limestone | 121/2 | A | Fdy. Re. | 2.29 | 0.79 | 3.43 | 3.45 | 0.077 | 0.073 | 0.067 | 2590 | 2640 | 20 |
| 21 | 4 Limestone | 19 | 9.9 | Re. 20 | 2.34 | 0.78 | 3.45 | 3.49 | 0.073 | 0.078 | 0.073 | 2630 | 2660 | 2 in. |
| 22 | 7 Limestone | 9.9 | 20 | Re. 21 | 2.20 | 0.75 | 3.49 | 3.52 | 0.078 | 0.081 | 0.078 | 2600 | 2620 | 2 in. |
| 23 | 7 Lst., 1 Spar | 99 | 2.0 | Re. 22 | 1.97 | 0.71 | 3.59 | 3.59 | 0.081 | 0.077 | 0.073 | 2600 | 2620 | 2 in. |
| 24 | 10 Lst., 2 Spar | 22 | 22 | Re. 23 | 2.25 | 0.76 | 3.59 | 3.72 | 0.077 | 0.066 | 0.059 | 2580 | 2600 | 31/2 in. |
| | 10 Lst., 4 Spar | 99 | 50 | Re. 24 | 2.09 | 0.67 | 3.72 | 3.71 | 0.066 | 0.072 | 0.070 | 2500 | 2580 | 21/2 in. |
| | 10 Lst., 4 Spar | 17 | A | Fdy. Re. | 2.28 | 0.78 | 3.41 | 3.47 | 0.081 | 0 070 | 0.066 | 2590 | 2630 | 4 in. |
| 31 | 3 Lst., 2 Spar | 121/2 | A | Re. 30 | 2.20 | 0.72 | 3.47 | 3.43 | 0.070 | 0.069 | 0.066 | 2500 | 2550 | 31/2 in. |
| 32 | 3 Lst., 4 Purite | 20 | 22 | Re. 31 | 1.62 | 0.61 | 3.43 | 3.41 | 0.069 | 0.064 | 0.059 | 2560 | 2580 | 3 in. |
| 33 | 3 Lst., 4 Purite | 17 | A | Re. 32 | 1.70 | 0.72 | 3.41 | 3.60 | 0.064 | 0.064 | 0.060 | 2590 | 2620 | 51/2 in. |
| 34 | 8 Lst., 4 Purite | 99 | | Re. 33 | 1.39 | 0.63 | 3.60 | 3.44 | 0.064 | 0.062 | 0.059 | 2620 | 2700 | 5 in. |

All heats melted 6 to 14 charges of iron pigged from a previous melt.

All analyses average of 3 to 8 samples.

Average temperature is the average of 7 to 15 optical readings on stream from end of trough. Maximum temperature is the average of 3 highest adjacent readings.

Slag fluidity measured in Herty viscosimeter with %-in. channel. Each number the average of 3 or more determinations.

allowed to drop to 0.43 per cent and the sulphur consequently increased to 0.304 per cent. These heats were excluded from calculations of the equilibrium level. In a similar manner carbon rose from a charge of 1.20 per cent C to an equilibrium level averaging 2.32 per cent C. This carbon level is much lower than experienced on foundry cokes of usual quality and may be attributed to the high ash content of the coke.

A similar group of nine remelts were made in the same cupola lined with magnesite brick. These are represented by the solid marks and lines in Fig. 2. The first 70 per cent steel charge that yielded a 0.219 per cent S in the acid cupola gave a 0.141 per cent S on the first basic heat. The average level of sulphur which was reached early and maintained was 0.128 per cent on the basic compared to 0.233 per cent on the acid group. On the basic series carbon continued to increase for four heats and then leveled out at an average of 3.10 per cent carbon for five remaining remelts. On the acid series carbon reached its level earlier on the second remelt and maintained a lower average of 2.32 per cent C for ten more heats. Slag fluidities on the acid heats were between 2 in. and 4 in., and on the basic heats consistently from 4 in. to 5 in. The results are summarized below. The basic

ACID-BASIC COMPARISON-REMELTS ON HIGH S COKE

| | | No. Remelts | Si Loss, | Mn Loss, | 0 | Equil. Carbon, | Equil. Sulphur, |
|-------|--------|----------------|-------------|-------------|------|-------------------|--------------------|
| Acid | Lining | 12 | 18.5 | 32.0 | 2.31 | 2.32 | 0.233 |
| Basic | Lining | 9 | 26.0 | 28.9 | 2.89 | 3.10 | 0.128 |

cupola reached equilibrium with a sulphur content 0.100 per cent lower than the acid cupola as is evident from the graph and the above table.

Table 9 contains the essential data on a number of short experimental basic heats melted with various fluxes. Charges of pigged foundry iron (2.00 per cent Si and 3.30 per cent C) were melted with foundry coke. Many of these earlier heats were too short and were subject to some irregularities that were later improved. Nevertheless, they make possible some comparison of desulphurization effectiveness of several of the familiar basic fluxes.

On the first two heats the flux was 7 per cent dolomite, over twice the normal quantity necessary to keep an acid cupola slagging properly. In both cases the final sulphur was essentially the same as charged, within the limits of sampling and analytical precision. Although sulphur was not decreased, the 0.020 per cent to 0.040 per cent S increase that is usually picked up from the coke was absorbed by the slag. Some desulphurization is actually accomplished by the slag if a sulphur increase is prevented and the S content of the charge is maintained. The slag analysis indicated lower basicity than expected.

The next heat, Heat 13, was fluxed with 2 per cent metallurgical lime. An increase of 0.013 per cent sulphur was experienced. In all probability the lower desulphurization efficiency was due chiefly to the insufficient quantity of basic flux and low volume of slag.

Heat No. 1 with 3 per cent dolomite and 4 per cent purite (fused sodium carbonate) lowered a 0.068 per cent S charge to 0.049 per cent S. Remelting this 0.049 per cent S metal with the same flux but using a sand bottom in the cupola was less effective, producing an increase of 0.010 per cent S. Melting again on a basic magnamix bottom lowered the sulphur slightly. From these results it became evident that a sand bottom can lower the basicity of the first slag and prove slightly detrimental to desulphurization on such short heats.

Since purite proved so effective as part of the flux, two heats were run using the fused sodium carbonate alone. Heat No. 18 with 4 per cent purite experienced an increase of 0.012 per cent S. The use of 8 per cent purite on Heat 19 experienced an increase of 0.005 per cent S. On these heats slags were no more basic and no higher in Na₂O or total sulphur than when sodium carbonate was half or less of the total flux. Apparently, only that concentration of Na₂O that can combine with other fluxes is retained. The excess evidently volatilizes in the cupola stack.

The next three heats using 2, 4, and 7 per cent limestone removed just enough sulphur to hold essentially the same content as charged, all between a 0.004 per cent decrease and a 0.005 per cent increase. The use of 1 per cent fluorspar with 7 per cent limestone (Heat 23) produced a decrease of 0.004 per cent S which seems to be a slight improvement over straight 7 per cent limestone. The use of 2 per cent spar with 10 per cent limestone and 4 per cent spar with 10 per cent limestone (Heats 24 and 26) was still more effective both removing 0.011 per cent sulphur. The spar produced the expected increase in slag fluidity.

These heats seem to indicate that with a basic lining the conventional basic fluxes readily yield a final sulphur of 0.050 to 0.080 per cent which is 0.020 to 0.040 per cent lower than would be experienced with an acid lining. However, from these short heats it became apparent that a more favorable slag composition would be necessary to obtain lower sulphur iron, and a greater degree of desulphurization sufficient to justify basic refractories. Several representative slag samples contained less than 0.225 per cent total sulphur and a silica content of 32 per cent to 44 per cent which kept the basicity ratio* down between 0.50 to 1.20. Obviously some of these slags were not very basic. One source of acid contamination was found to be some clay refractories knocked into the cupola charge from the top third of the stack. To eliminate this possibility the basic lining was extended to the charging door for all subsequent heats.

Since high sulphur conditions afford a better opportunity to compare desulphurization efficiency, charges of 70 per cent steel were melted with the high-sulphur coke "D" (coke properties in Table 8). This is about the worst condition conceivable as far as sulphur pick-up is concerned and might easily result in 0.300 to 0.500 per cent sulphur contents in acid cupola practice. These heats are described in Table 10.

On Heat 40 one of the best conventional flux combinations, 3 per cent dolomite and 1 per cent purite, was used on such a charge. The flux was tripled on the first bed charge and doubled for two more charges.

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^{*} Basicity ratio considered as Ca+MgO+Na2O

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Carbon, sulphur, and temperature trends on this heat are graphed in Fig. 3. Sulphur on the first ladle was 0.130 per cent and remained between 0.125 and 0.153 per cent throughout the heat for an average of 0.137 per cent sulphur. Carbon likewise came in at 2.32 per cent and remained between 2.32 and 2.46 per cent for an average of 2.38 per cent carbon. Temperature tapped at 2560 F because of a cold trough, climbed to 2730 F by the fifth ladle, and remained between 2730 and 2760 F for the remaining seven ladles of the heat. Average temperature was 2700 F and the maximum temperature averaged for three consecutive readings was 2750 F. Slag was very fluid, averaging 5 in. in the Herty type mold. Another effective flux combination, 10 per cent limestone and 2 per cent spar, was used on Heat No. 50 and followed a similar pattern averaging 0.139 per cent S and 2.34 per cent C.

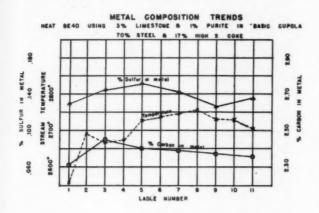


Fig. 3

On Heat 515, a 5 per cent addition of silicon carbide briquets was used along with 5 per cent limestone and 2 per cent spar. Certain benefits are claimed in the conventional cupola from the use of small percentages of this material. It was used here hoping its strong reducing power would improve desulphurization and carbon pick-up. The final carbon average of 2.59 per cent was slightly higher but the sulphur of 0.195 per cent was higher than without the silicon carbide. The temperature difference was within the limits of reproducibility. Approximately 1.25 per cent additional Si was recovered from 5 per cent addition.

The next several heats were designed to investigate the possibilities of calcium carbide within the cupola charge. For several decades small concentrations of calcium carbide have been internally produced in the desulphurizing slag in basic electric steelmaking. Recently it has been learned that cast iron can be desulphurized in the ladle with very fine carbide blown under the surface of the metal with an inert gas. No attempts to use this material within the cupola has been reported and carbide supposedly neither melts nor reacts at cupola temperatures.

On Heat 80, 5 per cent lump commercial carbide (1½ x 3% in.) was added along with 5 per cent limestone and 2 per cent spar with each charge of coke. The sulphur average of 0.088 per cent was much lower than any experienced on this high sulphur coke. In addition the final carbon of 2.74 per cent was about 0.40 per cent higher and the temperature was 50 to 80 deg higher than with any other flux. The lowest sulphur of 0.047 per cent, the highest carbon of 2.91 per cent, and the maximum temperature level of 2835 F were not attained till near the end of the short heat so that the averages do not reflect the real possibilities on a longer heat or with the carbide efficiently located.

Table 10-Experimental Desulphurizing Heats-High Sulphur Coke-70% Steel Charges

(All heats melted 15 charges consisting of 70% steel and 30% Pig. Theoretical charge composition: C 1.20%. S 0.060%, P 0.27%, and Si 2.50/2.70 and Mn. 0.85/1.00% after ferroallovadditions)

| Heat | | | | | Si, % | Mn, % | Carl | bon,% | | Sulphur,9 | 76 | Tem | p., F | Slag |
|------|----------------------------|-------|---|-------|-------|-------|-------|-------|-------|-----------|-------|------|-------|------------|
| No. | Flux, % | Coke, | % | Final | Final | Chg. | Final | Max. | Chg. | Final | Min. | Avg. | Max. | Fluid., in |
| 40 | 3 Dol, 1 Purite | 15 | D | 2.14 | 0.63 | 1.20 | 2.39 | 2.46 | 0.060 | 0.141 | 0.125 | 2700 | 2750 | 5 |
| 50 | 10 Lst., 2 Spar | 17 | D | 1.90 | 0.73 | 1.20 | 2.34 | 2.48 | 0.060 | 0.139 | 0.128 | 2730 | 2750 | 31/2 |
| 515 | 5 Si. Carb., 5 Lst., 2 Spa | ar 17 | D | 2.24 | 0.69 | 1.20 | 2.59 | 2.70 | 0.060 | 0.195 | 0.150 | 2730 | 2770 | 5 |
| 80 | 5 Carb., 5 Lst., 2 Spar | 17 | D | 1.72 | 0.79 | 1.20 | 2.74 | 2.91 | 0.060 | 0.088 | 0.047 | 2780 | 2835 | 6-3-1/ |
| 60 | 7 Carb., 1 Spar | 17 | D | 2.26 | 0.94 | 1.20 | 2.59 | 2.70 | 0.060 | 0.097 | 0.047 | 2800 | 2870 | 2 |
| 70 | 7 Carb., 1 Spar | 17 | D | 2.24 | 0.77 | 1.20 | 2.91 | 3.02 | 0.060 | 0.083 | 0.052 | 2800 | 2900 | 6-4-2 |
| 110 | 8 Carb. Briquet (6 in.) | 17 | D | 2.28 | 0.80 | 1.20 | 2.68 | 2.84 | 0.060 | 0.109 | 0.063 | 2730 | 2760 | 31/2-31/ |
| 90 | 8 Carb. Briq. (4 in.) | 17 | D | 2.05 | 0.68 | 1.20 | 2.44 | 2.68 | 0.060 | 0.164 | 0.119 | 2690 | 2750 | 7 |
| 120 | 8 Carb. Briq. (2 in.) | 17 | D | 1.81 | 0.70 | 1.20 | 2.80 | 3.11 | 0.060 | 0.124 | 0.085 | 2760 | 2790 | 7 |
| 150 | 7 Carb., 1 Sp. | 17 | D | 2.06 | 0.74 | 1.20 | 3.30 | 3.41 | 0.060 | 0.059 | 0.046 | 2790 | 2870 | 10-4 |
| 160 | 7 Carb., 1 Sp. | 12 | D | 1.98 | 0.72 | 1.20 | 2.64 | 2.77 | 0.060 | 0.111 | 0.067 | 2810 | 2880 | 8-6-1/ |
| 190 | 7 Carb., 1 Sp. | 9 | D | 1.93 | 0.73 | 1.20 | 2.72 | 2.84 | 0.060 | 0.093 | 0.074 | 2780 | 2840 | 9-5 |
| 170 | 7 Large Carb., 1 Sp. | 9 | D | 1.86 | 0.71 | 1.20 | 2.60 | 2.68 | 0.060 | 0.116 | 0.076 | 2860 | 2900 | 4-1/2 |
| 180 | 7 Large Carb., 2 Sp. | 6 | D | 1.99 | 0.66 | 1.20 | 2.39 | 2.49 | 0.060 | 0.172 | 0.124 | 2850 | 2880 | 31/2 |
| 508 | 5 Carb., 5 Lst., 2 Sp. | 17 | D | 2.01 | 0.75 | 1.20 | 3.24 | 3.49 | 0.060 | 0.061 | 0.032 | 2750 | 2800 | 8-3 |
| 514 | Eq. 6 Carb., 5 Ls. 2 | 17 | D | 2.31 | 0.72 | 1.20 | 2.85 | 2.94 | 0.060 | 0.083 | 0.066 | 2780 | 2840 | 7 |
| | Sp. in basin | | | | | | | | | | | | | |
| 513 | Eq. 6 Garb. (Bed) | 17 | D | 2.13 | 0.74 | 1.20 | 3.30 | 3.50 | 0.060 | 0.032 | 0.015 | 2850 | 2900 | 2-8 |
| | 5 Ls. 2 Sp. | | | | | | | | | | | | | |
| 516 | Eq. 6 Carb. (Bed) | 17 | D | 2.10 | 0.68 | 1.20 | 3.65 | 3.74 | 0.060 | 0.037 | 0.033 | 2870 | 2920 | |
| | 5 Ls. 2 Sp. | | | | | | | | | | | | | |

^{*} Except where indicated carbide used was calcium carbide in the form of small lumps 1½ in x 3½ in. Large carbides 3½ in. x 2 in Two or three figures for slag fluidity indicate definite change in fluidity on last half of heat. Each figure represents average of 3 or more fluidity measurements.

Eq. 6% Carb. means the total weight of carbide was equivalent to 6% of the total metal charge.

On the next heat, Heat 60, 7 per cent carbide was used with 1 per cent spar without any limestone. The average sulphur of 0.097 per cent was not quite as low as the previous heat with limestone because of a higher first sample. However, practically the same minimum sulphur of 0.047 per cent was reached toward the end of the short heat. Average carbon content was slightly higher and the temperature increased still higher than with the limestone. Again the effects were realized late in the short heat. The last half of the heat averaged 0.050 per cent sulphur from this charge that was yielding 0.130 to 0.287 per cent sulphurs with other flux combinations in the basic cupola. Carbon content was 2.58 per cent on the first ladle increasing to 2.94 per cent on the seventh ladle. This level was maintained for the last half of the heat. The average of 2.78 per cent carbon is at least 0.40 per cent carbon higher than comparable heats without carbide. The temperature increased steadily to a maximum level of 2870 F which was maintained for essentially the last half of the heat. This maximum temperature was 100 deg hotter than comparable heats without carbide and the average temperature of 2800 F for a 15 charge heat is over 60 deg higher than heats with usual fluxes.

Heat No. 70 using 7 per cent carbide and 1 per cent spar was essentially a duplication of Heat 60 with the exception of a heavier bed flux, equivalent to three times the regular flux. The composition trends of both metal and slag are graphed in Fig. 4. Aided by the additional carbide on the bed, the first sulphur was lowered some to 0.184 per cent. This dropped rapidly to a 0.054 per cent S on the fifth ladle and retained this low level throughout the heat. Again, carbon climbed to a near 3.00 per cent level which was maintained

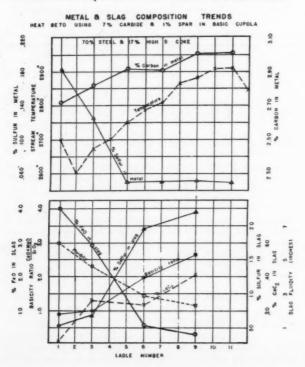


Fig. 4

from the fifth ladle to the end of the heat. Temperature continued to climb to a maximum level of 2900 F at the end of the heat.

Slag Composition

Changes in slag composition were also drastic from the third to the sixth ladle. The sharp decline in metal sulphur is easily accounted for by the rapid increase in slag sulphur, reaching a content of 2.20 per cent on the ninth ladle. Reasons for this more favorable distribution of sulphur between slag and metal is the key to desulphurization efficiency. The basicity increased as the heat progressed. This would be expected to improve desulphurization. Slag FeO decreased sharply and parallel to the S decrease in the metal. Under the conditions experienced in cupola melting the slag FeO appears to have some importance either directly or indirectly. Theories explaining the mechanism of sulphur pick-up in the cupola are controversial and incomplete. Regardless of the absorption mechanism, it appears evident that low sulphur metal can be obtained from a cupola if sufficient capacity for sulphur can be developed in an adequate quantity of free flowing slag. It further appears evident that a slag with such affinity for sulphur can be produced if the basicity is high and the FeO low. Since calcium carbide is both basic and reducing both these needs are ideally met resulting in a powerful desulphurizing agent when added in the cupola charge.

The increased carbon pickup from carbide is more difficult to understand. Whether the carbon is absorbed from direct contact of metal drops with the carbide, from hydrocarbon gases emitted from the decomposition of the carbide, or from an increased depth and intensity of the reducing zone may be debated. The increased temperature obviously suggests some exothermic reaction but most reactions involving carbide supposedly require a higher temperature than obtained in the cupola. Further research of a laboratory nature will be necessary to determine definitely the

reactions involved.

All three of these effects, sulphur removal, carbon pick-up, and increased temperature, are desirable in cupola melting. Additional expense has been incurred in order to realize each one of these individually. Removal of this much sulphur by ladle reagents requires not only the cost of the material but the cost of additional handling and refractory consumption in the case of sodium compounds. Such treatments cause a loss of temperature that is frequently a handicap to the foundry industry where high temperature, slag-free metal is needed at the entrance to the mold.

Means of increasing carbon pick-up are valuable for control purposes. In some applications requiring a high carbon iron, sufficient carbon content can not be obtained at times from the coke and metal available without use of special materials such as, lump graphite, pitch coke, special briquets, etc. The calcium carbide has increased carbon pickup considerably more than any of these special materials. Furthermore, increased carbon pick-up is important economically in that it permits the use of higher proportions of less expensive cast scrap and steel scrap.

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economic value. Increasing the temperature from the cupola requires increased fuel consumption and a sacrifice of output. The maximum temperature attainable from the cupola is necessarily limited by the fundamentals of the process. In many cases the cost of oxygen enrichment or superheated air has been justified to obtain a temperature above this limit or a better temperature-output combination. Small percentages of carbide have increased melting temperatures considerably above the ordinary limit of cupola melting with no sacrifice of output.

Because of its effectiveness along all three of these lines the remainder of the experimentation was directed toward understanding and applying small percentages of carbide on various types of charges. Through the cooperation of Linde Air Products Co., some carbide-fluorspar briquets were made. These were used in 8 per cent quantities of the next three heats. Whole 6-in. briquets were used on Heat 110, 4-in. fractions of briquets on Heat 90, and 2-in. lumps on Heat 120. All these had the same quantity of flux as the two previous heats using 7 per cent carbide and 1 per cent spar. From Table 10 it is clearly evident that the briquets were less effective than the bare carbide. Only parts of the characteristic effects of carbide were experienced.

The next five heats are intended to explore the heat producing phase of the reaction. While using 7 per cent carbide and 1 per cent spar, the coke amount was decreased in progressive steps from 17 per cent to 6 per cent in order to find what extent carbide can substitute for coke and still maintain average temperature. Heat No. 150 with 17 per cent coke "D" was identical with Heat 60 and Heat 70 except for a special bed flux. Three charges of carbide were spread through the coke bed for distance of 15 in. below the first metal charge producing the maximum effects of carbide earlier in the heat. The minimum sulphur was the same but the earlier action improved the average sulphur. The effect on carbon pick-up was doubled by this lower bed carbide.

On Heats 160 to 190 the same bed height, bed flux, and charge flux of 7 per cent carbide and 1 per cent spar was used but the coke cut in progressive steps from 17 to 12 per cent, 9 per cent, and 6 per cent.

It was feared that stack permeability might be seriously impaired by small quantities of coke and the small 1½ in. x 3% in. carbide. Some 3½ in. x 2 in. lumps of carbide were secured and the 9 per cent coke heat duplicated with the larger lump carbide. Little chemical difference was noticed from the larger carbide. Temperature was apparently higher when

larger carbide was used but stack permeability was improved permitting a larger air volume from the centrifugal blower. Using the larger carbide and reducing the coke to as little as 6 per cent, Heat No. 180 melted at a 2850 F average temperature and a maximum of 2880 F. Si and Mn losses were not increased drastically, but the sulphur increased further and the carbon decreased further. The results of the four progressive coke reductions from 17 to 6 per cent are summarized in Table 11.

Following results are apparent from each decrease in coke: Melting rate increased, carbon decreased, sulphur increased, and maximum temperature was virtually unaffected.

The first two effects would be expected. However, at first glance, increased sulphur from reducing the coke which is the source of the sulphur seems irreconcilable. However, slag analyses indicated higher FeO and lower sulphur contents in the slag as decreased amounts of coke were used. From these results it appears that when carbide is substituted for coke the reducing influence and desulphurizing effectiveness is sacrificed. The importance of reducing conditions and low slag FeO in desulphurizing is emphasized further.

The last four heats in Table 10 were designed to determine the most effective location for the carbide. When added with the metal charges the full effects of the carbide were not realized until about eight charges had been melted. Heat No. 508 is similar to Heat 80 with 5 per cent carbide, 5 per cent limestone and 2 per cent spar except for the three charges of carbide in

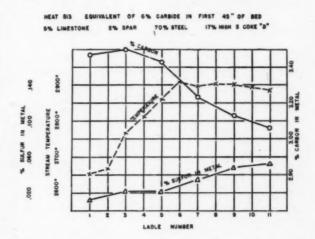


Fig. 5—Composition and Temperature Trends, Heat 513, 6% Carbide in Bed.

TABLE 11-EFFECT OF COKE REDUCTIONS-7% CARBIDE-1% SPAR FLUX-COKE "D"

| % Coke | Heat No. | Size Carbide | Avg. Carbon | Max. Carbon | Avg. Sulphur | Min. Sulphur | Avg. Temp. F | Max. Temp., l |
|-----------|-------------|-----------------|----------------|----------------|-----------------|-----------------|-----------------|------------------|
| 17 | 60 | Small | 2.79 | 2.95 | 0.097 | 0.047 | 2800 | 2870 |
| 88 | 70 | Small | 2.95 | 3.02 | 0.083 | 0.052 | 2800 | 2900 |
| 99 | 150* | Small . | 3.30 | 3.41 | 0.059 | 0.046 . | 2790 | 2870 |
| 12 | 160* | Small | 2.64 | 2.77 | 0.111 | 0.067 | 2810 | 2880 |
| 9 | 190* | Small | 2.72 | 2.84 | 0.093 | 0.074 | 2780 | 2840 |
| 9 | 170* | Large | 2.60 | 2.69 | 0.016 | 0.076 | 2860 | 2900 |
| 6 | 180* | Large | 2.39 | 2.49 | 0.172 | 0.124 | 2850 | 2880 |

the top 10 in. to 15 in. of the bed. Chemically the heat was more successful but the temperature was not as high. By adding more carbide within the 10-in. layer of green coke on top the bed, maximum effects were reached on the fifth charge.

This delayed effect plus the presence of unreacted carbide in the bottom drop suggests that the full effects are not realized until the carbide has worked its way below the metal being melted and into the coke bed.

On Heat 514 all the carbide was confined to the cupola basin. Large 31/2 in. x 2 in. carbide was added with the first bed coke and through the tuyeres in an amount equivalent to 6 per cent of the metal charge. The 5 per cent limestone and 2 per cent spar were added with the coke charges. All three of the effects of carbide were realized but not as effectively as on the latter half of the three heats on which carbide was added with the charges. Carbon averaged 2.85 per cent and sulphur 0.083 per cent. Maximum temperature level was 2840 F. Some of the characteristic reactions are experienced when the molten metal bath contacts the carbide in the basin of the cupola but not to the extent experienced when the reaction takes place in the melting zone.

On Heat 513 the carbide was distributed throughout the coke bed from the tuyere level to 45 in. above the tuyeres. In this zone the carbide was about onefourth the weight of the coke and the total weight was equivalent to 6 per cent of the metal charge. The carbide was most efficient in this location. The 3.30 per cent average carbon was the highest obtained and the 0.032 per cent sulphur the lowest average obtained on this high sulphur coke. To obtain a sulphur this low from this high S coke, the slag would have to remove about 0.200 per cent S. The chemical trends graphed in Fig. 5 indicate the carbide exerted maximum effect at the first of the melt and decreased in efficiency as the heat progressed.

The lowest sulphur of 0.015 per cent was obtained on the first ladle and sulphur content increased steadily to a 0.052 per cent at the end. A carbon level near 3.45 per cent was maintained for the first one-third of the heat and then decreased steadily to a 3.06 per cent at the end.

On Heat 516 large carbide was used in the same location as the previous heat. Essentially the same results were experienced. Carbon and temperature were higher from the larger carbide. Whether this is a direct result of carbide size or an indirect result of improved stack permeability is still open to question. On these heats carbide was somewhat too low in the bed and was partially depleted toward the end of the heat. More uniform results have been obtained by moving the carbide up about 20 in. higher in the stack starting about 20 in. above the tuyeres and continuing in the charges till about the tenth charge from the last.

The heats in Table 12 are similar to those in Table 10 except for the coke. Coke "A" of average foundry grade was used instead of the high-sulphur coke. Metal charges were the same 70 per cent steel, 30 per cent pig iron combination. With a conventional 3 per cent limestone, a 70 per cent steel charge (Heat 200) melted at an average sulphur content of 0.117 per cent. The effect of steel in the charge on increased sulphur pick-up is evident when compared with an all pig iron charge melted with the same flux and coke at an average sulphur of 0.063 per cent (Heat 82 in Table 13). The addition of 1 per cent purite to the 3 per cent

TABLE 12-DESULPHURIZING HEATS, 70% STEEL CHARGES, AVERAGE COKE "A"

| Heat | | | Si, % | Mn, % | (| Carbon, 9 | 0 | | Sulphur, | 70 | Tem | p., F. | Slag |
|------|-----------------------|---------|-------|-------|------|-----------|------|-------|----------|-------|------|--------|------------|
| No. | Flux, % | Coke, % | Final | Final | Chg. | Final | Max. | Chg. | Final | Min. | Avg. | Max. | Fluid., ir |
| 200 | 3 Limestone | 14 A | 1.98 | 0.64 | 1.20 | 2.72 | 2.82 | 0.060 | 0.117 | 0.107 | 2770 | 2810 | 31/2-7 |
| 210 | 3 Limestone, 1 Purite | 14 A | 1.76 | 0.53 | 1.20 | 2.59 | 2.70 | 0.060 | 0.106 | 0.100 | 2770 | 2790 | 7 |
| 220 | 7 Limestone, 1 Spar | 14 A | 1.79 | 0.59 | 1.20 | 2.78 | 2.94 | 0.060 | 0.102 | 0.088 | 2730 | 2770 | 6 |
| 240 | 5 Carb, 5 Lst., 2 Sp | 14 A | 1.89 | 0.70 | 1.20 | 3.31 | 3.51 | 0.060 | 0.054 | 0.023 | 2760 | 2840 | 8 |
| 280 | 7 Carb., 1 Purite | 14 A | 1.50 | 0.66 | 1.20 | 2.90 | 3.07 | 0.060 | 0.084 | 0.058 | 2720 | 2800 | 6 |
| 230 | 7 Carb., 1 Spar | 14 A | 2.07 | 0.69 | 1.20 | 3.09 | 3.20 | 0.060 | 0.075 | 0.049 | 2770 | 2825 | 7 |
| 260 | 7 Carb., 2 Spar | 14 A | 1.82 | 0.76 | 1.20 | 3.19 | 3.53 | 0.060 | 0.048 | 0.022 | 2700 | 2780 | 8 |
| 270 | 7 Large Carb., 1 Sp. | 14 A | 1.54 | 0.80 | 1.20 | 3.14 | 3.38 | 0.060 | 0.071 | 0.035 | 2730 | 2850 | 9 |
| 290 | 7 Large Carb., 1 Sp. | 9 A | 1.68 | 0.63 | 1.20 | 2.95 | 3.11 | 0.060 | 0.075 | 0.040 | 2710 | 2820 | 7 |
| 300 | 7 Large Carb., 1 Sp. | 4 A | 2.03 | 0.71 | 1.20 | 2.90 | 3.08 | 0.060 | 0.071 | 0.044 | 2795 | 2870 | 6 |
| 320 | 7 Large Carb., 1 Sp. | 4 A | 2.03 | 0.71 | 1.20 | 2.75 | 2.84 | 0.060 | 0.085 | 0.078 | 2770 | 2820 | 8 |
| 330 | 3 Large Carb., 1/2 Sp | 9 A | 1.86 | 0.74 | 1.20 | 2.54 | 2.63 | 0.060 | 0.109 | 0.094 | 2730 | 2790 | 6 |

TABLE 13-HIGH CARBON HEATS FOR NODULAR IRON, ALL PIG CHARGES
(All Charges 0.030% S and 4.20% C)

| Heat | leat | | | Mn, % | Carbo | on, % | Sulph | ur, % | Ten | np., F | Slag | |
|------|--------------------------------|----------|----------------|-------|-------|-------|-------|-------|------|--------|--------------|--|
| No. | Flux, % | Coke, % | Si, % Final | Final | Final | Max. | Final | Min. | Avg. | High | Fluidity, in | |
| 81 | 3 Limestone | 15 A | 1.57 | 0.15 | 3.90 | 3.92 | 0.073 | 0.064 | 2660 | 2700 | Fluid | |
| 82 | 3 Limestone | 15 A | 2.67 | 0.16 | 3.77 | 3.82 | 0.063 | 0.054 | 2680 | 2700 | Fluid | |
| 504 | 5 Limestone, 1 Spar | 14 A | 2.67 | 0.16 | 3.77 | 3.82 | 0.063 | 0.054 | 2680 | 2700 | Fluid | |
| 360 | 7 Limestone, 1 Spar | 14 A | 2.34 | 0.15 | 3.80 | 3.98 | 0.022 | 0.015 | 2650 | 2700 | Fluid | |
| 250 | 5 Carb., 5 Lst., 2 Spar | 14 A | 2.01 | 0.16 | 3.89 | 3.96 | 0.022 | 0.016 | 2680 | 2730 | 6-2 in. | |
| 310 | 5 Carb., 5 Lst., 2 Spar | 14 A | 2.39 | 0.22 | 3.67 | 3.77 | 0.021 | 0.011 | 2660 | 2750 | 6 in. | |
| 510 | 5 Carb., 5 Lst., 2 Spar | 14 A | 2.64 | 0.18 | 3.85 | 3.93 | 0.015 | 0.011 | 2650 | 2700 | 7 in. | |
| 511 | 5 Carb., 5 Lst., 2 Spar | 14 A | 2.77 | 0.15 | 3.97 | 4.04 | 0.014 | 0.009 | 2700 | 2730 | 5 in. | |
| 512 | 5 Carb., 5 Lst., 2 Spar | 14 A | 2.37 | 0.11 | 4.09 | 4.17 | 0.010 | 0.009 | 2660 | 2700 | Shut downs | |
| 517 | Eq. 6 Carb. (bed), 5 Lst., 3 S | par 14 A | 1.98 | 0.14 | 4.11 | 4.23 | 0.009 | 0.005 | 2710 | 2780 | 51/2 in. | |

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el of limestone lowered the sulphur to 0.106 per cent on the next heat. Sulphur was lowered an equal amount by increasing the flux to 7 per cent limestone and 1

per cent spar, for a 0.102 per cent S.

Carbide was used on the remaining nine heats. On Heat 280, 1 per cent purite with 7 per cent carbide was not as effective in any respect as I per cent spar with the same amount of carbide. On Heat 240 a flux of 5 per cent carbide, 5 per cent limestone, and 2 per cent spar was added with the charges. Sulphur ran as low as 0.023 per cent and averaged 0.054 per cent S for the entire heat. Although neither the temperature increase nor the carbon pick-up is as great as with the straight carbide and spar, this combination with limestone seems to produce lower sulphurs with better consistency. On Heat 270 little difference was found in the larger 31/2 in. x 2 in. carbide except possibly a slower effect on the first few ladles. On the next three heats, reductions in coke were made similar to those on the high sulphur coke in Table 10, and with similar results. On Heat 320 only 4 per cent coke was used, the maximum temperature of 2820 F and the average of 2770 F are still equal to or better than obtained with no carbide and 31/2 times as much coke. On Heat 330, with the carbide cut to 3 per cent, 9 per cent coke charges produced the same temperature and a faster melting rate than 14 per cent coke without carbide.

Table 13 contains a number of heats produced for experimentation with nodular graphite iron. For such treatment, chemical requirements are high carbon, low sulphur, low phosphorus, and low manganese. Charges were of low phosphorus pig iron with Si but no Mn added. The first two heats (Heats 81 and 82) using 3 per cent limestone as flux averaged 0.073 per cent and 0.063 per cent sulphur. Carbons averaged 3.90 per cent and 3.77 per cent. On the next two heats (Heats 504 and 360) increasing the flux to 5 per cent limestone and 1 per cent spar decreased the sulphur

average to 0.055 per cent; and an increase to 7 per cent limestone, 1 per cent spar decreased the sulphur further to 0.022 per cent.

On the next five heats 5 per cent carbide was added along with 5 per cent limestone and 2 per cent spar with each coke charge. Average sulphur on the five heats ranged from 0.010 per cent to 0.022 per cent. The last three with heavier carbide charges on top of the bed ran lower in sulphur than the first two. Sulphurs this low were ideal for this type of iron and the improved recovery saved enough magnesium alloy to justify the cost of the carbide.

On the last heat, Heat 517, the carbide was properly distributed throughout the bed in an amount equivalent to 6 per cent of the total charge. The balance of the flux, 5 per cent limestone and 2 per cent spar, was added with each charge. Maximum efficiency was realized with the carbide in the bed resulting in an average sulphur of 0.009 per cent and two samples as low as 0.005 per cent (determined by both combustion and

gravimetric methods).

The benefits of carbide are realized more completely and to greater practical advantage on the heats in Tables 14 and 15 melting all scrap charges and all steel charges. On Heat 340 a scrap remelt charge of 0.287 per cent sulphur was reduced to 0.100 per cent sulphur by 5 per cent carbide, 5 per cent limestone and 2 per cent spar on each charge. This removal of 0.187 per cent S represents good desulphurization efficiency from the carbide without considering the carbon pickup and temperature increase. On Heat 519, using an equivalent of 6 per cent carbide in the bed, a charge of 0.134 per cent sulphur was reduced to a 0.016 per cent sulphur average and a 0.010 per cent minimum. The temperature of 2815 F was over 100 deg hotter than usually experienced on such charges without carbide. A carbon of 3.83 per cent was obtained from a 2.30 per cent C charge. On Heat 522 with the carbide cut to

TABLE 14—HEATS MELTING CHARGES OF HIGH SULPHUR SCRAP (On all heats charge consisted of 100% high sulphur remelt, and 14% coke "A.")

| Heat | | Silic | on, % | Manga | Manganese, % | | Carbon, % | | Sulphur, % | | | Temp., F | | |
|------|---------------------------|-------|-------|-------|--------------|------|-----------|-------|------------|-------|------|----------|---------|--|
| No. | Flux, % | Chg. | Final | Chg. | Final | Chg. | Final | Chg. | Final | Min. | Avg. | Max. | Fluid | |
| 340 | 5 Carbide, 5 Lst., 2 Sp. | 2.50 | 1.79 | 1.00 | 0.81 | 2.20 | 2.89 | 0.287 | 0.100 | 0.075 | 2660 | 2740 | 9–2 in. | |
| 519 | Eq. 6 Carb. (bed), 5 Lst | , | | | | | | | | | | | | |
| | 3 Spar | 2.49 | 1.85 | 0.82 | 0.72 | 2.30 | 3.83 | 0.134 | 0.016 | 0.010 | 2760 | 2815 | | |
| 522 | Eq. 3 Carb. (bed), 5 Lst. | , | | | | | | | | | | | | |
| | 3 Spar | 2.75 | 2.31 | 0.99 | 0.89 | 2.48 | 3.62 | 0.119 | 0.031 | 0.024 | 2730 | 2780 | | |

Table 15—All Steel Charges, Average Coke (All heats melted 100% steel scrap charge with composition of 0.20 C, 0.050 S, 0.030 P, Si and Mn added to concentration indicated. 16% Coke "A" used.)

| Heat | | Silio | on, % | Manganese, % | | Carbon, % | | Sulphur | | Temp., F | | Slag | |
|------|---------------------------------------|-----------|-------|--------------|-------|-----------|------|---------|-------|----------|------|------------|--|
| No. | Flux, % | Chg. | Final | Chg. | Final | Final | Max. | Final | Min. | Avg. | Max. | Fluid., in | |
| 380 | 10 Lst., 1 Spar | 3.55 | 2.34 | 1.00 | 0.67 | 2.48 | 2.52 | 0.081 | 0.068 | 2760 | 2805 | 4 | |
| 503 | 5 Lst., 1 Spar (Ca Si)* | 3.25 | 2.08 | 1.10 | 0.99 | 2.48 | 2.81 | 0.119 | 0.115 | 2670 | 2725 | | |
| 506 | 2 Silicon Carbide, 7 Lst., 1 Spar | | 2.19 | 1.00 | 0.62 | 2.30 | 2.52 | 0.134 | 0.123 | 2695 | 2740 | | |
| 390 | 5 Silicon Carbide, 5 Lst., 1 Spar | | 2.60 | 1.00 | 0.77 | 2.41 | 2.50 | 0.116 | 0.107 | 2770 | 2800 | 7 | |
| 370 | 5 Carbide, 5 Lst., 2 Spar | | 1.27 | 1.00 | 0.85 | 3.17 | 3.31 | 0.048 | 0.028 | 2810 | 2920 | 5 | |
| 502 | 5 Carbide, 5 Lst., 2 Spar | 3.55 | 2.65 | 1.00 | 0.83 | 2.86 | 3.17 | 0.059 | 0.031 | 2800 | 2870 | 5 | |
| 505 | 5 Carbide, 5 Dol., 2 Spar | 3.55 | 2.76 | 1.00 | 0.85 | 2.94 | 3.40 | 0.066 | 0.035 | 2830 | 2895 | 8 | |
| 518 | Eq. 6 Carb. (bed), 5 Lst. | 3.00 | 2.67 | 1.00 | 0.88 | 3.52 | 3.63 | 0.025 | 0.015 | 2850 | 2935 | 6 | |
| 520 | Eq. 3 Carb. (bed), 5 Lst., 3 Sp. | 2.80 | 2.41 | 1.00 | 0.89 | 3.50 | 3.58 | 0.026 | 0.020 | 2885 | 2950 | 5 | |
| 521 | Eq. 11/2 Carb. (bed), 5 Lst., 3 Sp. | 3.00 | 2.40 | 1.00 | 0.85 | 3.19 | 3.37 | 0.047 | 0.039 | 2845 | 2880 | 8 | |
| | Ca Si used instead of Fe Si as source | e of sili | icon. | | | | | | | | | | |

an equivalent of 3 per cent in the bed a charge of 0.119 per cent sulphur was reduced to 0.031 per cent. A carbon of 3.62 per cent was obtained from a charge of 2.48 per cent. Desulphurization of this efficiency permits the use of larger proportions of higher sulphur

scrap.

The ten heats in Table 15 melted charges of 100 per cent steel scrap. All heats used 16 per cent coke "A" with varied fluxes. In the conventional cupola a 2.20 per cent C and a 0.140 per cent sulphur would be expected from such an all steel charge. On Heat 380 with 10 per cent limestone and 1 per cent spar in a basic cupola, carbon averaged 2.48 per cent and sulphur 0.081 per cent. On Heat 503, 5 per cent limestone and 1 per cent spar was added as flux but calcium silicon rather than ferrosilicon was used as the source of the 3.25 per cent silicon added. It was hoped that the calcium silicon would be more reducing and more basic and improve desulphurization effectiveness. However, the 0.119 per cent sulphur showed no improvement and the 2.48 per cent carbon was average for the basic cupola with conventional fluxes.

On the next two heats silicon carbide in briquet form was added again hoping that its strong reducing power would increase carbon pickup and desulphurization. On Heat 506, 2 per cent silicon carbide was used with 7 per cent limestone and 1 per cent spar. Sulphur averaged 0.134 per cent and carbon 2.30 per cent for no improvement over an average basic slag. On Heat 390, silicon carbide was increased to 5 per cent with 5 per cent limestone and 1 per cent spar. Sulphur averaged 0.116 per cent and carbon 2.41 per cent. It is possible that a slight improvement in temperature was experienced on this heat but hardly beyond the limits of experimental reproducibility.

On the remaining six heats calcium carbide was used. On Heat 370 and Heat 502, 5 per cent carbide with 5 per cent limestone and 2 per cent spar was added with each charge. Sulphur averaged much lower, 0.048 per cent and 0.059 per cent, and carbons much higher, 3.17 and 2.86 per cent. Maximum temperatures showed considerable increases, 2920 F and 2870 F. Both slags averaged 5-in. fluidity throughout

the heat. On Heat 505, dolomite was used instead of limestone with the same flux combination. Sulphur averaged 0.066 per cent, carbon 2.94 per cent, and the maximum temperature was 2895 F. Slag fluidity averaging 8 in. was higher but desulphurization efficiency was slightly less.

On the last three heats the carbide was properly extended into the coke bed ahead of the metal charges. From 20 in. above the tuyeres carbide was mixed with the bed coke, then with the individual coke splits until the tenth charge from the last. On Heat 518 the total amount of carbide was equivalent to 6 per cent of the metal charged. Sulphur averaged a much lower 0.025 per cent, with the first ladle as low as 0.015 per cent. Average carbon of 3.52 per cent was obtained from this all steel charge. The maximum temperature level of 2935 F is the highest experienced or found recorded for conventional cupola melting, especially on such a small diameter cupola where the radiation losses are greater.

On Heat 520, carbide was cut to the equivalent of 3 per cent of the metal charge. On this single heat effectiveness in carbon pickup, desulphurization, and temperature increase were decreased only slightly, in spite of reducing the carbide one-half. A 3.50 per cent C was still obtained from an all steel charge. Cutting the carbide still further to the equivalent of 1½ per cent still produced a gray iron composition of very high quality averaging 3.19 per cent C, 0.047 per cent S, 0.03 per cent P, and 2880 F maximum temperature. Higher losses of Si and Mn are usually experienced from the use of steel scrap. However, it is apparent that the losses of both Si and Mn were cut to less than half by the use of carbide.

Utilization of this extreme carbon pickup makes possible another method of lowering phosphorus content, through increased proportions of steel scrap. Such a practical possibility could have sufficient economic value to more than justify the cost of the carbide and basic lining, especially in view of nodular iron developments requiring very low phosphorus contents. Low phosphorus irons, freight included, cost Southern foundries \$15.00 to \$25.00 gross ton more than local

TABLE 16-SLAG ANALYSES-BASIC DESULPHURIZING HEATS

| Heat | Ladle | | Metal | Sulphur | | | | | Slag (| Composit | ion, % | | | | |
|------|-------|------------------------|-------|---------|------------------|---------------|--------|--------|--------|----------|--------|------------------|------------------|------|----------|
| No. | No. | Flux, % | Chg. | Sample* | SiO ₂ | $Al_{a}O_{a}$ | FeO | MnO | CaO | | NagO | CaF ₂ | CaC ₂ | S | P_2O_1 |
| | | | | | Hig | H SULPH | UR COK | E "D" | | | | | | | - |
| 40 | 5 | 3 Dol., 1 Pur. | 0.078 | 0.153 | 31.5 | 15.6 | 9.9 | 3.7 | 20.5 | 18.1 | 2.2 | | | 0.14 | |
| 46 | 3 | 3 Dol., 1 Pur. | 0.103 | 0.116 | 39.9 | 18.3 | 6.5 | 3.5 | 18.3 | 8.7 | 2.0 | | | 0.29 | |
| 50 | 9 | 10 Lst., 2 Spar | 0.078 | 0.135 | 24.5 | 10.2 | 7.6 | 3.0 | 37.2 | 16.6 | | 1.76 | | 0.72 | |
| 70 | HB | 7 Carb., 1 Sp. | 0.078 | 0.184 | 34.2 | 18.4 | 4.0 | 2.2 | 16.7 | 23.7 | | 0.90 | | 0.52 | |
| 70 | 3 | | 0.078 | 0.126 | 34.8 | 19.9 | 2.9 | 1.7 | 22.8 | 17.6 | | 0.80 | 0.27 | 0.71 | |
| 70 | 6 | 7 Carb., 1 Sp. | 0.078 | 0.054 | 22.0 | 24.7 | 0.6 | 0.7 | 33.6 | 18.3 | | 0.80 | 0.24 | 1.98 | |
| 70 | 9 | 7 Carb., 1 Sp. | 0.078 | 0.052 | 21.4 | 22.3 | 0.3 | 0.4 | 47.2 | 8.5 | | 1.12 | 0.41 | 2.20 | |
| 80 | 9 | 5 Carb., 5 Lst., 2 Sp. | 0.078 | 0.051 | 23.3 | 9.6 | 0.8 | 0.5 | 54.4 | 4.6 | | 1.9 | 0.39 | 1.88 | |
| | | | | | REGI | LAR GRA | DE CO | KE "A" | | | | | | | |
| 1 | 5 | 3 Dol., 4 Pur. | 0.068 | 0.046 | 38.8 | 14.5 | 4.6 | 3.7 | 20.5 | 18.1 | 2.2 | | | 0.14 | |
| 200 | 8 | 3 Limestone | 0.078 | 0.114 | 28.6 | 14.3 | 6.5 | 2.6 | 25.7 | 21.1 | | 0.84 | | 0.34 | 0.37 |
| 210 | 8 | 3 Lst., 1 Pur. | 0.078 | 0.102 | 29.5 | 14.3 | 6.6 | 3.1 | 21.3 | 22.4 | 1.1 | 0.40 | | 0.24 | 0.35 |
| 360 | 9 | 7 Lst., 1 Spar | 0.030 | 0.018 | 31.6 | 8.1 | 4.2 | 0.3 | 36.7 | 13.5 | | 1.40 | 0.03 | 0.14 | 0.14 |
| 380 | 9 | 10 Lst., 2 Spar | 0.050 | 0.068 | 24.8 | 4.7 | | | | | | | | | |
| 513 | 3 | Eq. 6 Carb. (bed), | | | | - | 5.3 | 3.1 | 34.95 | 23.7 | | 1.70 | | 0.49 | 0.18 |
| | | 5 Lst., 2 Spar | 0.050 | 0.019 | 22.6 | 2 | 1.2 | 0.5 | 53.0 | 7.8 | | 1.40 | 0.85 | 0.94 | 0.11 |
| 513 | 9 | Eq. 6 Carb. (bed), | | | | | | | | | | | | 197 | |
| | | 5 Lst., 2 Spar | 0.050 | 0.043 | 21.0 | 2 | 1.3 | 0.3 | 53.1 | 9.5 | | 1.00 | 0.94 | 1.07 | 0.11 |

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high phosphorus iron. Heat 520 produced a 3.50 per cent C, 0.026 per cent S, 0.03 per cent P iron from \$32.65 gross ton steel scrap. Ordinarily an iron of this composition would require low phosphorus pig iron costing \$63.50 gross ton.* This \$30.00 difference in base metal cost left at least a \$12.00 per net ton cost advantage on this particular heat after allowance for \$7.88 for silicon, \$3.60 for carbide, and a \$2.40 estimate for the basic lining, extra flux, etc.

Actually the conventionally produced iron would be at least 0.040 per cent higher in sulphur. Ladle treatments to remove this amount would add more to its cost and drop the ultimate temperature to a level 200 deg below the carbide-steel heat.

Analyses of slags from some of the experimental desulphurizing heats are given in Table 16. The sulphur content of the slags varied from 0.14 per cent to 2.20 per cent on the heats using high-sulphur coke and from 0.14 per cent to 1.07 per cent on the heats using low-sulphur coke. The four samples from Heat 70 (previously graphed in Fig. 4) emphasize the importance of slag composition in desulphurization. Metal sulphur dropped progressively from 0.184 per cent to 0.052 per cent as the sulphur of the slag increased from 0.52 to 2.20 per cent. The changes in slag composition provide some explanation for the improvement in metal-slag distribution during the heat. The total of the basic constituents CaO and MgO increased while the acid SiO2 decreased progressively. Another concurrent trend is the gradual decrease of FeO as the heat proceeded.

Slags from more of the heats are being analyzed with the hope of establishing some slag metal relationships that are needed for a better understanding of cupola chemistry both basic and acid. Slag chemistry is a subject within itself and further discussion will be withheld until some future paper when a sufficient volume of analyses can be presented and more explanation of the reactions given.

Summary

Our experience on over 100 heats in basic-lined cupolas may be summarized briefly. Magnesite brick have proven practical as a cupola lining material. Refractory consumption was less than one-third that of clay brick on 9-hr production melts and less than one-fifth on certain types of experimental heats that are ordinarily very destructive to refractories. The improved refractory life should in many applications justify most of the increased cost of basic refractories. A small basic-lined cupola was operated successfully on a production basis with no operational troubles and with consistent chemical control. Its operation required only a recognition of differences in refractory properties, slag consistency, and cupola chemistry.

The basic lining permitted extremes in slag chemistry on experimental heats which produced valuable changes in metal chemistry and a degree of flexibility that has never been associated with cupola melting.

Melting in the conventional cupola retains all phosphorus and absorbs more sulphur resulting in both economic and metallurgical limitations on cupola metal. In the basic-lined cupolas both phosphorus and sulphur have been removed but most effectively by separate treatments.

From 0.10 to 0.32 per cent phosphorus has been removed in the cupola from 0.30 to 0.80 per cent P irons by several oxidizing treatments plus a highly basic slag. The cheapest source of oxidation was the combustion condition resulting from a low coke ratio. Iron ore in the charge permits the use of more coke with a slightly higher temperature. Manganese ore is more expensive but advantageously retains higher carbon, higher Mn, lower sulphur, and a better slag condition. Sodium carbonate seems still more effective but produces the lowest carbon content. Removal of all the Si is not necessary for dephosphorization at these P levels but much lower Si contents are required than usually present in gray iron compositions. Low temperature and restoration of the Si are the chief limitations of the dephosphorizing process.

Sulphur content is lowered some by the naturally increased basicity of the slag from a basic-lined cupola. From 0.020 to 0.040 per cent lower sulphur pick-ups are obtained from the same charge at usual sulphur levels, and as much as 0.100 per cent lower at high sulphur levels. Further decreases in sulphur were found to require lower slag FeO as well as higher basicity necessitating stronger reducing conditions than normally encountered in the cupola.

Greatest desulphurization efficiency resulted from the use of lump commercial calcium carbide within the cupola charge. Small percentages of carbide within the coke bed have efficiently desulphurized the metal below the content of the charge along with several other advantages. Some of the sulphur contents obtained are:

Removal of 0.187 per cent S from a 0.287 per cent S all scrap charge with 5 per cent carbide.

A 0.016 per cent S final from a 0.134 per cent all scrap charge with 6 per cent carbide.

A 0.031 per cent S final from a 0.119 per cent S ali scrap charge with 3 per cent carbide.

A 0.026 per cent S and 3.50 per cent C from an all steel charge with 3 per cent carbide.

A 0.047 per cent S and 3.19 per cent C from an all steel charge with $1\frac{1}{2}$ per cent carbide.

A 0.009 per cent S final from an all pig iron charge with 6 per cent carbide.

Sulphur contents were obtained lower than any reported in cast iron by any production method and equal to basic electric steels. Such desulphurization efficiency should permit use of more high sulphur scrap by foundries and extend the application of the cupola in duplex steel making.

In addition to efficient desulphurization the carbide produced an increased carbon pick-up, an increased temperature, and a decreased loss of oxidizable elements. The increase in carbon pick-up has been sufficient to produce 3.20 to 3.50 per cent C iron from all steel charges. Such carbon absorption provides indirectly a second means of lowering phosphorus through the use of higher proportions of steel, and with consid-

Prices current at the time of heat and f.o.b. Birmingham, Ala. The economics would vary according to the district and the raw material market.

erable economic advantage.

The increase in temperature was sufficient to melt at 2900 F charges that could not be melted over 2800 F without the carbide. This heat producing ability has permitted coke reductions to one-third normal with the same melting temperature and output increased 40 per cent. The use of small percentages of carbide makes possible an increased melting rate without any sacrifice of temperature or an increased temperature without any sacrifice of melting rate.

The results obtained remove many of the long time limitations of cupola melting and make possible the production of iron suitable for nodular treatment by this most economical melting furnace.

Acknowledgment

The author wishes to acknowledge assistance and cooperation of the following groups whose combined efforts made this work possible: Melters P. L. Nelson and C. B. Long for assistance in running the heats; T. W. Harwell of Linde Air Products Co. for supplying various forms of carbide; the entire laboratory staff of the American Cast Iron Pipe Company for the analyses; H. W. Dent and E. M. Whelchel for preparation of graphs; Dr. J. T. MacKenzie, J. A. Bowers, and C. K. Donoho for advice and support; and the management of the American Cast Iron Pipe Co. for the financial support of research of this type.

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DISCUSSION

Chairman: A. E. Schuh, U. S. Pipe & Foundry Co., Burlington, N. I.

Co-Chairman: C. O. Burgess, Gray Iron Founders' Society, Cleveland.

Co-Chairman Burgess: As far as producing ductile iron is con-

cerned, this development is very significant in that the cupola becomes an increasingly valuable instrument for melting the required base iron. I recall that Morrogh noted that calcium in addition to magnesium promotes the nodular form of graphite. I wonder whether any observations have been made of that some in connection with the use of calcium carbide at the American Cast Iron Pipe Co.

MR. CARTER: Some iron, mostly nodular in structure, has been produced in our plant with calcium additions. We believe that a low sulphur is important in the mechanism. With the sulphur content reduced as low as some of these basic-cupola irons it seems quite likely that some other elements will produce nodular graphite, elements that have failed on higher sulphur irons because of their insufficient desulphurizing ability. Calcium continues to be a tantalizing possibility, but has not yet given us complete success on a practial basis.

R. A. FLINN: 1 I would like to add our expression of appreciation to the author for his fine presentation of very valuable data. There are two questions I wish Mr. Carter would elaborate on.

What is the mechanism of the high temperatures accompanying reduced coke and the addition of calcium carbide? Is there an exothermic reaction from the carbide?

Secondly, I noticed some of the desulphurization curves and the temperature seemed to be quite important. You probably already have considered temperature along with the basicity, FeO content, and other variables.

MR. CARTER: Several possibilities were suggested in the literature on the mechanism of the carbide reaction. One possibility suggested to us is a reaction with atmospheric moisture forming acetylene which burns. Experience with carbide in the cupola basin suggests that the effects experienced can result from direct contact with the metal. Improved effectiveness in the melting zone might be due to better contact with the drops of molten metal and slag as they are first melted. Although not recorded, there remains some evidence that carbide might simply burn in air under cupola conditions.

Any one of these reactions or any combinations would conceivably be reducing to both metal and slag, give off heat, and leave a basic residue of calcium oxide for the slag, all of which are favorable effects. I believe the complete explanation can be obtained better under laboratory conditions that will permit the elimination of some of the variables present.

In answer to the question on the effect of temperature on desulphurization, we had only indirect evidence that temperature was favorable to desulphurization. When the temperature was highest, desulphurization was generally greatest but conditions were generally more reducing and slag basicity the highest also, so that it was difficult to isolate the independent effect of temperature. From theoretical considerations and other experiences temperature would be expected to favor desulphurization, I believe.

CHAIRMAN SCHUH: It seems that what was done here was that the author experimented with high temperature chemistry using the cupola and that he uncovered considerable information. As the author mentioned perhaps more refined laboratory type studies with variables better controlled will elucidate the mechanisms that would account for the results obtained.

¹ Metallurgist, American Brake Shoe Co., Mahwah, N. J.

A THERMODYNAMIC STUDY OF PINHOLE FORMATION IN STEEL CASTINGS *

By

Robert E. Savage** and Howard F. Taylor ***

PINHOLES are among the more serious surface imperfections suffered by steel castings; they are particularly prevalent in steel castings made in green sand molds. In 1938 Sims and Dahle solved the problem for the industry by determining that aluminum used as a deoxidant was a satisfactory antidote for pinholes. Aluminum was an excellent deoxidizer and being soluble in iron an excess could be added as insurance against gas pickup during or after pouring.

Unfortunately, carbon-molybdenum steel castings, used for high temperature-high pressure service, began to fail by graphitization at welded joints. Aluminum was found to promote graphitization and was barred by specifications from use in making this type of steel. To date no suitable substitute has been found. In one plant alone repair of pinholes costs \$200,000 per year, so the problem is acute and merits serious study.

This paper renews the investigation into pinhole formation with a thermodynamic appraisal of the problem. It is recognized that the problem is more one of kinetics than thermodynamics, but the kinetic approach is not currently possible because of the lack of dependable data. It is hoped the study will reawaken interest and spark investigations which will ultimately prove this or some other tenable theory.

The genus "pinhole" embraces a variety of surface imperfections in steel castings. Pinholes are small, round, or elongated, smooth-walled gas holes occurring just under, or barely pricking through, the skin of a steel casting. Figures 1 and 2 are a photograph and a sketch of typical pinholes. The long axis of the pinhole is perpendicular to the casting surface. The layer of steel covering the holes is usually thin, so thin that heat treatment oxidizes away the skin and uncovers the defect. Some pinholes are clean and some are filled with a slag-like substance. It is not definitely known that all pinholes have the same origin; for example, the pinholes containing slag may be initiated by the

particles of slag acting as a nucleus while the clean holes may require a higher gas pressure. At any rate, the defect is one readily recognized by steel foundrymen.

Rimholes of rimming steel ingots have the same shape and similar position in relation to the ingot surface as do pinholes in steel castings. Hultgren and Phragmen¹ show that elongated, thin blowholes in rimmed ingots are caused by carbon monoxide, although there are always small quantities of hydrogen and nitrogen in the gas evolved. Caine² concluded that pinholes in steel castings are caused by evolved gases, and that the gas responsible may be either hydrogen or carbon monoxide. Chipman,3 Sims,4 and Sims and Zapffe⁵ show the improbability of the formation of carbon monoxide in dead-killed steel for castings. Swinden and Stevenson⁶ give evidence that hydrogen can cause porosity in silicon-killed steels. Sims and Zapffe⁵ have attributed pinhole porosity to hydrogen dissolved in the steel.

Hydrogen As a Cause of Pinholes

Sims and Zapffe⁵ describe pinhole formation as follows:

As soon as the metal is poured into a green sand mold, the iron combines with water vapor liberated at the mold-metal interface. This reaction of iron and water vapor produces iron oxide and hydrogen, and



Fig. 1-Pinhole porosity in a steel casting (Briggs).

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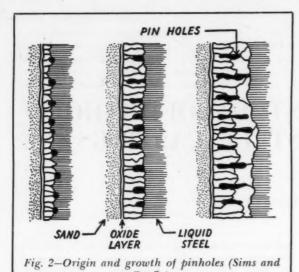
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^{*} Part of thesis for Doctor of Science Degree in Metallurgy Department at Massachusetts Institute of Technology.

^{**} Metallurgist, International Nickel Co., Inc., New York, and *** Associate Professor, Metallurgy Department, Massachusetts Institute of Technology, Cambridge, Mass.



Zapffe).

the hydrogen readily diffuses through the thin skin of metal which has solidified into the molten metal just under the skin. The hydrogen-iron oxide reaction is upset and water vapor and iron reform. In other words, the reaction given by Equation (2) proceeds to the left initially and to the right finally. Aluminum prevents this by reducing iron oxide to such a low level that the equilibrium is not upset.

Hydrogen may be dissolved by the steel from the atmosphere, particularly if the humidity is high. Thermodynamically the reaction for the breakdown of water vapor to hydrogen on oxygen is

$$2H$$
 (in Fe, 0.00024%) + O (in Fe, 0.0085%) = H_2O (g, 0.00658 atm) $\Delta F = -42400 + 29.1T$ Eq. (1)

The amount of water in ordinary atmosphere at 25°C and 20 per cent relative humidity is used for the computation. The free energy of the reaction given by Equation (1) is positive at 1535 C which indicates that the reaction proceeds from right to left. Therefore, a breakdown of water vapor to oxygen and hydrogen dissolved in the steel is possible even from the air surrounding the metal in a ladle. The driving force of the reaction is even higher for the steam forming at the mold-metal interface. Hydrogen diffuses readily through liquid or solid steel, so absorbtion through the solid skin is highly probable.

The carbon-water vapor reaction appears more likely to produce dissolved hydrogen than the iron-water vapor reaction. The changes in free energy for the reactions (assuming reactants and products in their standard states) are given by Equations (2) and (3). Carbon monoxide and hydrogen would form preferentially to iron oxide and hydrogen under these conditions.

Fe (1) + H₂O (g) = FeO (1) + 2H (in Fe)
$$\triangle$$
 F°₁₈₀₀₋₂₀₀₀°_K = +18.650 + 12.82T Eq. (2)
C (in Fe) + H₂O (g) = CO (g) + 2H (in Fe) \triangle 1728-1972°_K = 34,120-33.66T Eq. (3)

The activities of the hydrogen and oxygen used above are typical values from assays of molten steel. The activity of hydrogen used is the per cent H found in a 0.35 carbon steel deoxidized with aluminum. All other hydrogen values listed in this reference were less than 0.00024 per cent. The activity of the oxygen used is the per cent O in equilibrium with 0.5 per cent silicon in steel at 1873°K (1600°C). The water vapor pressure was assumed at 5 mm of mercury (25°C, 20 per cent relative humidity), and the reaction was calculated at 1535°C (1808°K).

The following equations illustrate more specifically why hydrogen does not play a major role in pinhole

formation:

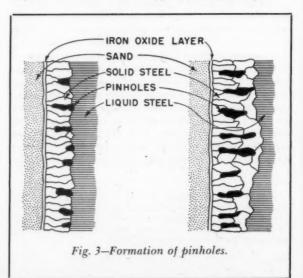
FeO (1, act.1) + 2H (in Fe, 1%) = Fe (1, act.1) + H₂O (g, 1 atm) \triangle F° = -13,500 - 15.98T Eq. (4) Fe (1, act.1) + O (in Fe, saturated) = FeO (1, act.1) \triangle F - O Eq. (5) 2H (in Fe, 0.00024%) = 2H (in Fe, 1%) \triangle F = +33.0T Eq. (6) 2H (in Fe, 0.00024%) + O (in Fe, saturated) = H₂O (g, 1 atm) \triangle F = -13,500 + 17.02T Eq. (7)

The conditions under which reactions (4) to (7) occur may be stated as follows:

Equilibrium or near equilibrium conditions are assumed. The surface skin of steel solidifies immediately upon contact of the molten metal with the sand; however, the liquid-solid interface moves slowest at reentrant angles where pinholes seem to be most prevalent. Therefore, it can be assumed that conditions nearer equilibrium prevail at reentrant angles immediately under the initial skin surface. Pinhole porosity is a subsurface phenomenon which occurs in the liquid state. Calculations are made for dry sand molds because the constants for transient absorbtion of hydrogen, as in a green sand mold, are not available.

In reaction (5), iron saturated with oxygen means that iron is in equilibrium with iron oxide. The surface layer of the steel becomes saturated with oxygen according to the reaction $O_2 = 20.$ * Enough oxygen must be present so that free FeO forms at the casting

[•] This is strictly true only in the absence of moisture in the sand. In the presence of moisture, the reverse of reaction (1) may also contribute dissolved oxygen in the initial stages.



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skin. When more oxygen dissolves in the steel, the steel is supersaturated in oxygen and iron oxide will precipitate. Reaction (6) transposes dissolved hydrogen at 1 per cent, the standard state activity, to 0.00024 per cent, an actual percentage analyzed in a 0.35 per cent carbon steel deoxidized with aluminum.

By combining reactions (4) to (6), the over-all reaction between actual concentrations of H and O at the liquid-solid interface is given by reaction (7). The value of \triangle F at 1808° K (1535° C) for this reaction is +17,300 calories. Therefore, the reaction cannot proceed to the right with the formation of water vapor at one atmosphere pressure, which is the minimum pressure necessary to precipitate a bubble. From these calculations, it appears that dissolved hydrogen is not the cause of pinholes in steel castings, at least to the extent that water vapor bubbles are nucleated in the liquid state.

In reaction (7), a hydrogen analysis of a 0.35 carbon steel deoxidized with aluminum was used. The use of this hydrogen content of 0.00024 per cent is justified, since the greatest hydrogen content in the skin possible under actual foundry conditions will not be great enough to permit formation of water vapor bubbles at one atmosphere pressure when a casting is poured in a dry sand mold. If the air atmosphere contains water vapor at 30 mm mercury pressure (30.5°C and 100 per cent relative humidity), and the casting is poured at 1950°K (1677°C), then the equilibrium H content determined from reaction (1) is 0.00062 per cent. These conditions should be the most likely to produce dissolved hydrogen in a steel casting poured in a dry sand mold under actual foundry conditions. If the temperature of the liquidus is 1700°K (1427°C), and all the hydrogen dissolved at 1950°F (0.00062 per cent) remains in the skin of the steel, then the pressure of the water vapor bubble would be 0.067 atmospheres. This pressure would not be great enough to form a water vapor bubble.

Hydrogen is less likely to form gas holes at the interior of a casting, since the oxygen concentration is much lower than at the surface of the casting. By similar combination of reactions, an over-all reaction can be written:

2H (in Fe, 0.00024%) + O (in Fe, 0.0048%) =
$$H_2O$$
 (g, 1 atm) \triangle F = -42500 + 40.1T Eq. (8)

The hydrogen content and water vapor pressure are the same as in reaction (7). The oxygen content is 0.0048 per cent, which is the oxygen content in equilibrium with a steel containing 0.5 per cent silicon at 1808°K (1535°C). With these specific conditions, water vapor will not form at one atmosphere in a steel casting.

Carbon Monoxide As a Cause of Pinholes

Carbon monoxide appears more likely to form pinholes in steel castings than hydrogen. To study the role of dissolved carbon in pinhole formation, calculations similar to the ones for hydrogen are:

FeO (1, act.1) + C (in Fe, 1%) = Fe (1, act.1) + CO (g, 1 atm)
$$\triangle$$
 F° = +20,400 - 20.05T Eq. (9)
Fe (1, act.1) + O (in Fe, sat.) = FeO (1, act.1) \triangle F = O Eq. (10) C (in Fe, 0.3%) = C (in Fe, 1%) \triangle F = +2.4T Eq. (11)

C (in Fe, 0.3%) + O (in Fe, sat.) = CO (g, 1 atm)
$$\triangle$$
 F = +20,400 $-$ 17.65T Eq. (12)

All conditions under which reactions (4) to (7) apply are valid in using reactions (9) to (12) with the replacement of the hydrogen content by a carbon content of 0.3 per cent. The over-all reaction between C and C at the liquid-solid interface would be given by reaction (12). The value of Δ F for this reaction at 1808°F (1535°C) is -11,500 calories. Therefore, the reaction can proceed to the right, with the formation of carbon monoxide at one atmosphere pressure. The fugacity of CO in equilibrium with 0.3 C and C (saturated) at 1808°K, is 24.5 atmospheres. These calculations indicate that dissolved carbon, reacting with oxygen, is the cause of pinholes in steel castings. The pressure of carbon monoxide would be great enough to form gas bubbles in liquid steel.

Carbon monoxide is less likely to form gas holes at the interior of a casting, since the oxygen concentration is much lower than at the surface. The over-all reaction can be written:

C (in Fe, 0.3%) + O (in Fe, 0.0048%) = CO (g, 1 atm)
$$\triangle$$
 F = $-8,500$ + 5.46T Eq. (13)

This reaction is different from reaction (12) in the O content. The oxygen content is 0.0048 per cent, which is the same oxygen content used in reaction (8). With these specific conditions, carbon monoxide cannot form at one atmosphere in the interior of a steel casting.

In green sand molds pinholes are more likely to occur from the carbon monoxide reaction than in dry sand molds; the rate of decarburization is higher and the driving force for the reaction is higher. This is in accord with experience where pinholes are more prevalent in green than in dry sand practice.

Other Gases As Causes of Pinholes

All dissolved gases must be considered as possible contributors to the main cause of pinhole formation in steel castings. A partial listing of the gases includes:

It has generally been assumed that one gas forms a bubble in liquid metal and that all other gases diffuse into the void; this is based on the assumption that the partial pressure of the diffusing gases was zero in the original bubble. This assumption may not be valid. Each dissolved element tends to form a gas bubble for each reaction at a certain fugacity; a gas bubble will form whenever the summation of all fugacities from all reactions reaches a critical value. It would appear, then, that all gas-forming reactions must be considered sooner or later. However, it is doubtful if the summation of effects of all gases other than carbon monoxide would be great enough to cause a pinhole in the complete absence of the carbon monoxide reaction.

Conclusions

This thermodynamic study of pinhole formation indicates:

- 1. That hydrogen dissolved in steel does not create enough gas pressure to form cavities.
- 2. The hydrogen-oxygen reaction with formation of water vapor does not occur.
- 3. The most probable cause of pinholes is carbon-monoxide gas.
- 4. Carbon-monoxide gas is formed by reaction of the carbon of the steel with iron oxide formed at the mold-metal interface.

(In the presence of dissolved aluminum pinholes do not form because the surface oxygen reacts with the aluminum preferentially to the carbon.)

5. No experimental proof of the thermodynamic indications is offered; plant and laboratory tests have only recently been initiated. It is hoped new interest will be kindled in this important problem, and that concrete proof may be found to eliminate pinholes from castings made of metal which cannot be treated with aluminum.

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DISCUSSION

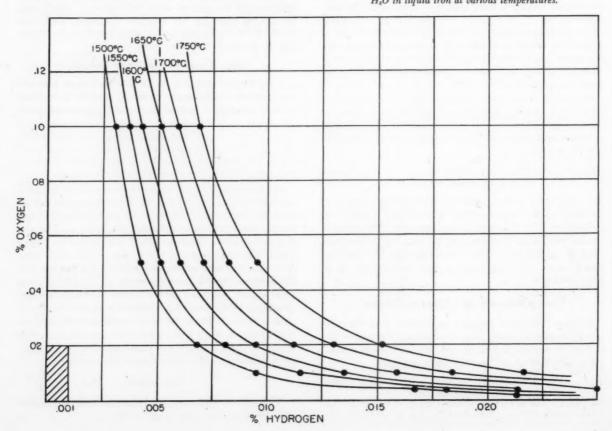
Chairman: C. W. Briggs, Steel Founders' Society of America, Cleveland.

Co-Chairman: R. E. Kerr, Pettibone Mulliken Corp., Chicago. C. E. Sims (Written Discussion): ¹ The authors have made an interesting approach to the problem of pinhole formation, and any method that will throw new light on the subject is welcome. On the basis of thermodynamic calculations, some sweeping conclusions were made, although a fair sized loop hole was left in conclusion No. 5. It is believed, however, that the evidence is far from conclusive and that the authors have been somewhat unrealistic in their analysis of the mechanisms involved.

The thermodynamic equations and calculations themselves seem to be all right although they are difficult to follow, because, unfortunately, (1) the source of the constants used were not given, (2) some of the equations are obviously for the standard state, while others are in doubt (probably a typographical error), and (3) some of the equations are for specific conditions such as definite concentrations and cannot readily be used to calculate for other conditions.

Murray Udy and the writer made some calculations based on the recent work of Dastur and Chipman.* From their equation

Fig. A—Hydrogen vs oxygen to give one atmosphere pressure of H₂O in liquid iron at various temperatures.



¹ Assistant Director, Battelle Memorial Institute, Columbus, Ohio.

^{* &}quot;Equilibrium in the Reaction of Hydrogen with Oxygen in Liquid Iron," Journal of Metals, August 1949, p. 441.

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 $P_{\rm H_2O}$ $\log K_1 = 7050/T - 3.17$, the value of $K_1 =$ for any

 $P_{BB} \times \%O$ temperature may be found. Then by fixing the pressure of H_2O at one atmosphere, the pressure of H2 may be found for any given oxygen content. Using Sievert's law, the hydrogen in solution for any given hydrogen pressure may be determined. From such calculations, the curves of the accompanying Fig. A were

In this figure, the curve showing the hydrogen and oxygen contents that would produce one atmosphere pressure at 1500 C is the most pertinent. This temperature is close to the liquidus for a medium carbon steel and, inasmuch as pinholes form after freezing starts, the liquid steel must necessarily be at the liquidus temperature. This curve seems to indicate plainly that it would be virtually impossible to have an H_BO boil, analagous to a CO boil in a steel bath, even under strongly oxidizing conditions.

HYDROGEN AND OXYGEN CONTENTS

The hydrogen and oxygen contents of a killed steel will usually fall within the cross-hatched area near the origin. The remoteness of this area from the curve for 1500 C would seem ample to justify the conclusions of the authors that water vapor is an unlikely source of pinholes. It should be mentioned here that the H2 pressures necessary to maintain the conditions for the 1500 C curve at equilibrium will be from 7 to 9 atmospheres for the lower oxygen contents. Hydrogen, however, never seems to work independently to initiate bubble formation. Nevertheless, the above evidence is unconvincing because there is abundant circumstantial evidence to the effect that hydrogen does play a najor part in the porosity of cast steel and that the oxygen content or degree of deoxidation is also an important factor. In an attempt to explain the discrepancy, it can be pointed out that the curves described are for values in liquid iron. No data are available to show what the relations would be in a steel containing carbon, manganese, and silicon. More important still, they do not consider the possible and probable high concentration of both oxygen and hydrogen at the solid-liquid interface of the frozen skin where pinholes begin to form.

The authors, in the last paragraph of page 393, postulate that the surface of the liquid steel must become saturated with oxygen in order for free FeO to form at the casting skin. This is unrealistic from several viewpoints. In the first place, the steel could not become so saturated without first oxidizing all of the silicon. Secondly, while the surface of liquid, flowing steel undoubtedly absorbs oxygen from the ambient atmosphere, the surface film is not stable but is being constantly broken up and absorbed into the body of the liquid. This reaction results in loss of deoxidizer, but usually there is no serious depletion except in the case of aluminum. Sometimes the steel that flows to the farthest reaches of a casting has pinholes from loss of aluminum, while portions nearer the gate are sound. Thirdly, the oxide layer can form readily after the skin freezes and then it will remain in place.

Along with the saturation for oxygen, the authors allow no increase in the hydrogen content, which would certainly not be the case if water vapor played a part in the oxidation.

Any postulated mechanism for pinhole formation must be able to reconcile the conditions known to influence pinhole formation. It must explain, among other things, (1) why pinholes start to form after a thin but apparently unbroken skin of frozen steel has formed in contact with the sand, (2) why moisture in the sand plays such an important part in pinhole formation, and (3) why very small additions of aluminum to a steel already deoxidized with silicon and manganese will usually prevent pinholes under adverse conditions.

SILICON-KILLED MEDIUM-CARBON STEEL

Let us consider the case of a silicon-killed medium-carbon steel with ordinary hydrogen content poured into a dry sand mold. As soon as the liquid steel comes to rest against the sand, a thin skin freezes and begins to increase in thickness with the formation of columnar grains at first. At the solid-liquid interface, there will be a concentration of the dissolved impurities including oxygen and hydrogen. When the concentration gets high enough, oxides (silicates high in FeO content) will precipitate, but the concentration of oxygen and hydrogen never get high enough for them to react. There is no evolution of gas and no pinholes form.

Next, consider the same steel poured into a green sand mold where the moisture content is high or where a reentrant angle aggravates the effect of moisture. Shortly after the skin forms, bubbles are initiated and grow into pinholes as the freezing wall advances. What makes the difference? Logically, it can be blamed on the moisture which turns to steam when heated by the steel. This steam is highly oxidizing to the hot skin of the casting and the reaction $H_aO+Fe \rightleftharpoons 2H^++FeO$ takes place.

Something must have penetrated the frozen skin to produce the pinhole reaction. The FeO formed by the surface reaction forms a layer of scale which could not penetrate in the time available because of the slow rate of oxygen diffusion in steel. On the other hand, the atomic hydrogen, formed in contact with the iron lattice, has a very high rate of diffusion in steel close to its freezing temperature and could diffuse readily through the surface skin. Of the two possibilities, hydrogen has the better chance of penetrating and building up a high concentration at

the solid-liquid interface.

The reaction between water vapor and iron is reversible and does not go to the completion in either direction. What is more logical than to expect this high concentration of hydrogen to react with some of the FeO rich oxides being precipitated at the solid-liquid interface and form a small quantity of water vapor? The solid oxide provides a good surface for bubble formation as well. The size of this bubble of water vapor needs only to be very small because, as was mentioned earlier, the hydrogen may be present with a pressure up to 7 or 8 atmospheres and would quickly precipitate into this bubble to give it a rapid growth.

Consider a third case in which the same steel is given an aluminum addition that will provide a residual content as small as 0.002 per cent. If it is poured into a green sand mold, the water vapor will oxidize the skin in the same way and hydrogen will diffuse through in equal quantity. The oxides that precipitate, however, are relatively inert FeO · Al₂O₀ or Al₂O₀, which will not react with hydrogen. Without the trigger action to start bubble formation, the hydrogen just passes on into the liquid

steel and becomes dilute.

For the reaction $C + O \rightleftharpoons CO$ to take place and form pinholes, the silicon would first have to be oxidized to a low value, and this would require a large quantity of oxygen. To oxidize 0.28 percent of silicon, that is to decrease the content from 0.50 per cent to 0.22 per cent, would require 0.32 per cent of oxygen. It would be difficult for this much oxygen to get in as a result of water vapor reaction or any other action. Furthermore, in view of the amount of oxygen involved, it would be even more difficult to postulate how a minuscule quantity of aluminum, which would be oxidized ahead of the silicon, could inhibit the reaction which causes pinholes.

CAUSE OF PINHOLES

Until new facts are disclosed, it appears that hydrogen is a more likely cause of pinholes than CO, thermodynamics notwithstanding.

J. B. CAINE (Written Discussion): 2 May the writer welcome a couple of converts to his side? His theory of carbon monoxide as one of the gases causing pinholes in steel castings, as advanced ten years ago was defeated at that time, not only by pure numbers, but also by the thermodynamic erudition of the opposition. Now the carbon monoxide side has a numerical superiority of three to two, plus some welcome thermodynamics.

Unfortunately, I must immediately start a civil war about conclusion 3 of the paper. The authors state "the most probable cause of pinholes is carbon monoxide gas." The authors have misquoted me slightly in that I concluded that "pinholes steel castings were caused by either evolved hydrogen or carbon monoxide." Let us add to this, or both. This may seem like

quibbling, but the "or both" is quite important.

The authors have confined themselves, and rightly so, in the interest of clarity, to the thermo-chemical side of pinhole porosity. There is also an equally important thermo-physical side of this problem, the physical rejection of hydrogen during solidification. Unfortunately from all standpoints, including that of simplicity, when steel is in contact with dissociated water vapor, it simultaneously absorbs both hydrogen and oxygen.

³ Technical Consultant, Wyoming, Ohio.

Hydrogen to possibly be physically rejected on subsequent solidification, oxygen to possibly react chemically with the carbon during solidification to form carbon monoxide gas. It is possible for both of the gases to be rejected during solidification, one physically, one chemically, and in any proportion depending on the conditions prevailing at that particular time.

The problem is then one not of statics, but of dynamics. Not only does the oxygen vary from surface to center as shown in Equations (12) and (13), but also all elements, for the steel is freezing and there must be an increase in all solutes between the frozen dendrites due to segregation. The only possible exception is hydrogen. Therefore, superimposed on the oxygen increase due to dissociation of water vapor there is an additional increase in carbon and oxygen contents due to segregation during solidification. This increase will increase the fugacity of CO to above the 24.5 atmospheres quoted in the paper. Unfortunately, from the debate angle, silicon segregates just like carbon and not enough is known about the 4-component system Fe-C-Si-O to say definitely what shift in equilibrium conditions occurs during solidification. Therefore, just what gas or gases are rejected in the interior of the casting cannot be established.

The surface is a different matter. There is evidence that the surface of porous steel castings, especially those poured in green sand is so highly oxidized that all the aluminum, and most of the silicon are gone. The evidence is metallographic; only manganese silicate inclusions are present. If aluminum were present in this surface metal, alumina inclusions would be seen. If the aluminum was all oxidized, but the silicon was present, the inclusions would be silica. The presence of manganese silicate, and in some cases iron sulphide rather than manganese sulphide inclusions, denotes a high level of oxidation, that the O in Equation (12) is at least approaching saturation and that the fugacity of the CO is approaching 24.5 atmospheres.

If only a tiny bubble of CO is formed, it then offers the opportunity for hydrogen to diffuse into it. As hydrogen cannot initiate a bubble by itself, the pinhole must always contain some gas other than hydrogen. As the hydrogen level in the metal is determined by entirely different variables than the carbon and oxygen level, the composition of the gas is controlled by many independent factors.

From a practical standpoint it is important that we do not swing over and blame all pinholes on carbon monoxide and forget all about hydrogen. All melters here realize that if they do not boil they will be in trouble with pinholes, regardless of the amount of aluminum added. Many realize that if the heat is overoxidized to obtain a boil and not subsequently deoxidized somehow in the furnace that more pinholes will be encountered. Perhaps this type of pinhole is due to carbon monoxide. If an excessive amount of aluminum is used instead of furnace deoxidation trouble may be encountered with rock candy fracture and poor fluidity.

This is again the matter of walking the straight and narrow between two extremes. The path becomes narrower if no aluminum can be added, but its the same old path.

P. C. ROSENTHAL'S DISCUSSION

P. C. Rosenthal (Written Discussion): ^a It is disturbing to note that Savage and Taylor in this paper, and Sims and Zapffe^a in an earlier one, using essentially the same basic thermodynamic data, have reached almost opposite conclusions regarding pinhole formation in cast steel. Sims and Zapffe concluded that a hydrogen-FeO reaction caused pinholing, whereas here we are told that this reaction is not possible, but that the formation of carbon monoxide is responsible.

The argument used by Sims and Zapffe against the possibility of a C-FeO reaction was that the silicon in the steel was sufficiently more reactive with oxygen than was carbon at or near the freezing point so that its preferential oxidation prevented carbon monoxide formation. This conclusion is based on data representing equilibrium conditions, a state of affairs which is far from realization when steel is poured into a mold. If carbon can be protected from oxidation by the silicon present, so also should be iron, yet iron oxide is produced at the steel skin.**

Therefore, it should not be impossible to have CO formed during the non-equilibrium cooling of cast steel, even when the silicon content is supposedly high enough to prevent this reaction. Sims and Zapffe did concede in the discussion of their paper that CO could form in some steels on rapid cooling.

Savage and Taylor have used this point of a high oxygen concentration at the surface of the steel to build a case for the C-FeO reaction and against the H-FeO reaction, but in so doing they have overlooked the possibility of considerably greater quantities of hydrogen present than their calculations indicate. They use as their basic hydrogen content a value of 0.00024 per cent. which was the highest obtained by Derge, Peifer and Richards on a sample of solid steel. This hydrogen content must be decidedly less than that present in the liquid state since hydrogen solubility is much less in the solid than in the liquid, and additional hydrogen is lost by diffusion from the solid. If the authors can assume saturation of the melt with oxygen, certainly it would be expected that hydrogen values, more nearly representative of actual conditions in molten steel, should also be assumed. Further, they have studiously avoided the possibility of any hydrogen coming from the dissociation of moisture in the sand or water of hydration from clay. Sims' and Zapffe's arguments were based largely on green sand molds, and the atmosphere created there would definitely favor the absorption of much more hydrogen than Savage and Taylor postulate.

If hydrogen contents higher than those used by Savage and Taylor are assumed, the free energy change given by Equation 7 could change from a positive to a negative value. Thus, with a hydrogen content of 0.0026 per cent (which is the theoretical value in solution in iron at about 1600 C [2912 F] under a pressure of one atmosphere of H₂). Equation 6 would give $\triangle F = 23.74$ T. If Equation 7 is corrected for this hydrogen content, the final free energy change would be $\triangle F = -13500 + 7.76$ T. At 1808 K, $\triangle F$ would then be only +530 calories. Since hydrogen is available both from the molten steel and from the surrounding atmosphere, momentary hydrogen contents at the surface much greater than 0.0026 per cent are conceivable.

Rather than ruling out the H-FeO reaction as a possible cause of pinholing, it appears the authors have shown thermodynamically the possibility of a C-FeO reaction. This point of view, as already stated, is not new. It would appear that either reaction is possible depending on conditions in the mold, and it should be entirely possible that in some instances both reactions could occur simultaneously.

The confusing issues that have arisen from the application of thermodynamics must indicate to the layman either than thermodynamics is an obliquitous science to be viewed with suspicion or that, like other tools of science, its limitations have not been fully recognized.

There is a great danger that the reputation of the thermodynamics approach will be placed in jeopardy by continuing to use it as the primary proof for a theory. It is believed that the rightful function of thermodynamics in problems of this type is to support concrete experimental evidence rather than to act as the evidence itself.

C. A. ZAPFFE'S DISCUSSION

C. A. ZAPFFE (Written Discussion): While this is a commendable study of an important problem, the thermodynamic factors can be presented in another way, and perhaps more informatively.

First, there is the fact that the infusion of [H] is initiated by an external stream-metal reaction in which $P_{\rm H20}$ is very likely of the order of 1 atm as a minimum value. One cannot therefore proceed to calculate an internal $P_{\rm H20}$ having a lesser value, for the intervening metal wall constitutes a diaphragm permeable to one or both components of the gaseous thermodynamic system.

Therefore, a principal point in question in the thermodynamic approach of the authors follows from their choice of an arbitrary analytical value for total hydrogen content of the metal. Analytical values for hydrogen are uncertain; but, more importantly, the control of the physico-chemical system is vested in the fugacity of atomic hydrogen, P_H, which is not disclosed by an analysis for total occluded gas.

In a paper* published subsequent to the reference cited by

^{*} The Mechanism of Pin-Hole Formation, by C. E. Sims and C. A. Zapffe, Transactions, A.F.A., vol. 49 (1941).

^{**} Discussion of Sims and Zapffe paper.

Assistant Professor, University of Wisconsin, Madison, Wis.

^{* &}quot;Concept of the Hydrogen Potential in Steam-Metal Reactions," by C. A. Zapffe, Transactions, A.S.M., vol. 40, pp. 315-52, disc., 352-4 (1948).

4 Consulting Metallurgist, Baltimore, Md.

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the authors, the thermodynamics for steam-metal reactions were formulated in more detail, showing how moisture reacting with metals may produce greater chemical activities or fugacities of atomic hydrogen than pure hydrogen gas itself, and by a factor of many fold. These calculations provide a ready explanation for any gas effusions noted during solidification in the castings under question, and they specifically answer the well-known and numerous relationships of moisture to gas problems in castings. Pinholes are directly influenced by the presence of moisture, not of carbon. Steels and irons virtually all contain carbon, but pinholing only occurs when there is carelessness in the foundry with regard to moisture-in the charge, mold, or atmosphere.

One particularly notable factor is missing in the author's treatment, and that is the quadratic relationship between [H] and (H₂), which runs up the pressure of precipitating hydrogen gas at an exponential rate with respect to the solubility decrease.

During solidification, the solubility of hydrogen in the iron decreases by a factor of several times. A threefold decrease between the solid growing crystal and the adjacent liquid in their Fig. 2 and 3 would generate a percipitation pressure of $P_{\rm H_2}$ \times $(3)^{3}$ or approximately ten times that $P_{H_{2}}$ present at the time of infusion. Since it has just been mentioned that the thermodynamic value for $P_{\rm H_2}$ at the reaction site may be much greater than one atmosphere where a moisture reaction is concerned, one can understand that ample force is available for development of the pinhole in moisture-contaminated metal, with or without carbon present. This conclusion conforms perfectly with the outstanding fact of pinholing in aluminum and magnesium and their alloys, where no carbon is present and where pinholes are clearly a hydrogen phenomenon.

J. H. Lowe's Discussion

J. H. Lowe (Written Discussion): 5 This is a very interesting paper. However, one thing puzzles me-the statement, "Carbon monoxide is the most probable cause of pinholes and is formed by reaction of carbon of the steel with iron oxide formed at the mold-metal interface."

If this is true, why are dynamo 0.05 to 0.12 per cent carbon steel heats the most susceptible to porosity, especially in green sand than other heats? There is relatively little carbon present to react with iron oxide compared with other steel heats, yet it is the most difficult to control even though 3 to 43/4 lb Al/ton are used as deoxidizer.

H. B. Myers: 6 A few years ago, I was associated with a steel foundry equipped with both acid open hearth and acid electric melting facilities. It was our experience in making the same type and composition of casting from acid open hearth steel and acid electric steel, in order to avoid pinhole porosity, we could

deoxidize the acid open hearth steel with aluminum in the order of none to 4 oz per ton; whereas, it was necessary to use aluminum in the order of not less than 2 lb per ton to deoxidize the acid electric steel.

I would like to ask the authors to explain how their theories are tenable with the foregoing situation. That is, why is it necessary to use more aluminum in acid electric steel than in acid open hearth steel of the same composition (elements normally analyzed for, such as: C, Mn, Si, S, P) to avoid the occurrence of pinhole porosity in the same types of castings? If the authors could go further and consider the basic open hearth, basic electric, and induction methods of melting, it would be quite informative.

Mr. SAVAGE: Do you have the oxygen content of the steel

melted in the acid open hearth or basic open hearth furnace?

MR. MYERS: No. The foregoing remarks are the result of observation only.

MR. SAVAGE: It would be difficult to give a final answer to this question without a complete chemical analysis. The oxygen content of the steel when melted in the acid or basic open hearth furnace would be one important factor with respect to the amount of aluminum necessary for deoxidation.

C. H. LORIG: 1 You might have another variant which would help explain the difference in susceptibility of electric and open hearth steel to pinhole porosity. It is true that in general the nitrogen content of electric furnace steel is higher than that of open hearth steel. It might be then that open hearth is less susceptible to pinhole porosity because of its lower nitrogen content.

H. F. TAYLOR (Author's Closure): The authors are grateful for the valuable discussions of their paper. Indeed, an important aim was to spark just such discussion and reawaken interest in the pinhole problem, which is responsible for much casting repair each year.

To answer the authors in detail would require more space than the original paper; although worthwhile, our answers would be as speculative as their discussions since quantitative data are not sufficient for rigorous treatment of the subject.

We do not believe the arguments offered weaken the case for pinhole formation as it is described in the paper. The discussions do highlight the many things we have yet to learn. It is certain that we have not solved our pinhole problem by considering water vapor as the main culprit. It seems essential for us to do some new thinking about pinholes-their cause and their cure.

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CASTING OF MAGNESIUM-RARE EARTH -ZIRCONIUM ALLOYS IN SAND MOLDS*

By

K. E. Nelson** and F. P. Strieter**

DEVELOPMENTS in the field of aircraft engines are toward higher operating temperatures. This means that materials used in these engines must have good properties at these temperatures. Another important factor to consider is the strength to weight ratio of the materials in the engine. Magnesium alloys, being the lightest of the structural metals, have been used successfully in the past in many applications. These alloys, however, which are primarily from the magnesium-aluminum-zinc system are unsuited for stressed applications at temperatures greater than about 300 F because their resistance to creep decreases as the operating temperature is increased. A new system of alloys has been developed, the magnesium-rare earth-zirconium alloys, which possesses considerably improved characteristics in this respect.

The treatise on magnesium alloys by Beck¹ reports data on both rare earth- and zirconium-containing wrought magnesium alloys. This book describes some of the work done on magnesium alloys in Germany during the years 1926 through 1938. The monograph of Haughton and Prytherch,² published in 1937, also describes some European developments on wrought magnesium-rare earth alloys. Murphy and Payne,³ Ball,⁴ and Sully⁵ have published information on a magnesium-³ per cent rare earth-³ per cent zinc-0.7 per cent zirconium composition designated ZRE1, for which they claim easy castability as well as good properties at room and elevated temperatures up to 400 F.

An intensive survey on the sand cast properties of hundreds of magnesium alloy compositions containing manganese, zinc, zirconium, and rare earth has been made at Dow for about the past ten years. The results of some of this work have been reported by Leontis and Murphy,⁶ and Leontis.⁷ The foundry characteristics of a large number of these compositions have been evaluated. From the information obtained, it was pos-

sible to select two systems to be developed as sand casting compositions: (1) magnesium-zinc-zirconium, and (2) magnesium-rare earth-zirconium. The magnesium-zinc-zirconium system is of considerable interest for sand castings requiring high strength and toughness at room temperature. These alloys will be covered in a later paper. Incidentally, this system is also the one which was selected a few years ago for development as a high strength wrought alloy. Dow started manufacturing ZK60 alloy (magnesium-6 per cent zinc-0.5 per cent minimum zirconium) extrusions for aircraft and other applications about four years ago. In the ensuing years more than a half million pounds of this alloy has been cast into extrusion billets, and a considerable portion of this has been extruded and sold. The magnesium-rare earth-zirconium group of alloys has proved the best system from which to choose a composition for applications at elevated temperatures. After a great deal of experimental work in this alloy system, a composition was selected for development as a commercial sand casting alloy. This composition, which has been designated as EK30, contains 2.0 to 4.0 per cent rare earth and 0.1 to 0.4 per cent zirconium. Its room temperature and elevated temperature properties are comparable to other compositions within this system of alloys, and it possesses the advantage that it can be handled in the foundry by the same general practice as is used for other commercial magnesium casting alloys. Subsequent field tests of engine parts cast in EK30 alloy have proved successful, indicating the choice of alloy composition was sound.

This paper is intended to present our experience to date on the metal handling, casting, and the tensile and creep properties of EK30 alloy at room and elevated temperatures. The development of an improved magnesium alloy to be used in high temperature applications is in its early commercial stage, and it is to be expected that beneficial modifications may be made as research in this field progresses.

Metal Handling

Melting practice is an important phase of operation in all types of foundries. In handling magnesium alloys containing rare earths or zirconium for sand

^{*} The term rare earth refers to the use of Mischmetal, a typical analysis of which is 50 per cent cerium, 20 per cent lanthanum, 18 per cent neodymium, 6 per cent praseodymium, and lesser amounts of other rare earths and impurities.

^{**} Magnesium Laboratories, The Dow Chemical Company, Midland, Michigan.

¹ All references will be found at the end of the paper.

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v

castings, it is, perhaps, even more important than with other alloys. To simplify handling of the metal, a practice has been adopted of supplying the foundry with ingot of two different compositions. When these are mixed in the correct proportions, an alloy possessing the proper analysis for EK30 will be obtained.

The ingot supplied consists of KO alloy (magnesium-0.5 per cent zirconium, minimum) and either E7 alloy (magnesium-7 per cent rare earth) or E10 alloy (magnesium-10 per cent rare earth). The E7 and E10 alloys are produced with a low iron content so that they will not reduce the zirconium from the KO when the two compositions are mixed in the same crucible. The flux used to protect the rare earthcontaining alloys from oxidation during melting and handling is Dow No. 220 flux which contains no magnesium chloride. The rare earths will react with any magnesium chloride present in the crucible, thereby lowering the alloy content of the melt. The alloying of zirconium has been described by Doan and Ansel,8 and Marande⁹ has discussed the alloying of the rare earths with magnesium. Slight modifications of their prescribed techniques are now in use.

When E7 is used, this alloy and KO are added to the charge in equal parts; but when E10 is used, only one part of it is charged to two parts of KO. After the charge is melted it is immediately heated to 1350 to 1400 F and thoroughly stirred. A dusting of 1.5 to 2.0 per cent No. 220 flux on the melt during the stirring operation removes nonmetallic inclusions from the metal. The treatment with chlorine gas, an excellent metal refiner for magnesium-aluminum base alloys, is not desirable because the rare earths react with the magnesium chloride which is formed. After refining, enough flux is added to wet the entire surface and a layer of fluorspar (calcium fluoride) approximately 1/8 in. thick is applied. The melt is then held at 1350 to 1400 F for a minimum of 20 min and it is then adjusted to the correct temperature. The flux cover is removed in the same manner as is recommended for the other magnesium casting alloys¹⁰ and the metal is poured into the molds.

Dow No. 181 agent and sulphur dioxide gas can be used with EK30 alloy during pouring and in the mold to inhibit oxidation, as with other commercial magnesium casting alloys. It is preferred, in crucible practice, to completely empty the crucible at least every third melt. This is done to keep the excess fluid flux

from taking too much crucible space and to reduce the possibility of flux contamination in the metal.

Aluminum, iron, hydrogen, manganese, nickel, and silicon will combine separately and possibly collectively with zirconium in molten magnesium. The compounds formed are insoluble in magnesium and will be precipitated out of the melt, resulting in a lowering of zirconium in the melt. Also, the addition of zirconium to a melt of magnesium-aluminum-zinc, such as AZ63 or AZ92 alloys, may coarsen the grain size of these melts and make conventional grain refining difficult. It is, therefore, an important consideration when handling and casting magnesium-aluminum base and magnesium-zirconium base alloys in the same areas that no inter-contamination should be tolerated, either through the interchanging of melting tools, metal charge, or in any other way.

A simple spot test has been developed to facilitate the identification of unknown alloys. With this test it is possible to separate magnesium alloys containing rare earths from magnesium alloys containing aluminum and/or zinc. The test consists of first obtaining a clean metal surface on the piece of alloy to be checked. This clean surface may be obtained by grinding, sanding, or sawing. A drop of dilute hydrochloric acid solution is placed on the cleaned area and, after a few seconds, a drop of 3 per cent hydrogen peroxide is placed over the same area as the acid. The appearance of a yellowish foam indicates the presence of rare earth in the alloy. If none is present, the precipitate will vary from gray to black depending on the amounts of aluminum and/or zinc present. Commercially pure magnesium and KO alloy will show a clear bright spot with no precipitate.

In order to check the quantitative effect of additions of aluminum to EK30 alloy, three 60-lb melts were prepared using standard procedures. Two of these melts were given identical treatments which consisted of melting 30 lb each of E7 and KO, heating to 1400 F, and stirring thoroughly. Each melt was then held 15 min at 1400 F and sampled. Then aluminum was added at eight different times, providing a total aluminum addition of 0.32 per cent. After each addition of aluminum, the melt was held 15 min before sampling and this was followed by the next aluminum addition. Table 1 lists the results of the aluminum addition on the analysis of EK30 alloy. Notice that both the soluble and insoluble zirconium drop as the aluminum.

TABLE 1-EFFECT OF SMALL ALUMINUM ADDITIONS ON ANALYSIS OF EK30 ALLOY

| | | | | | | * | Chemie | cal Analy | sis | |
|----|---|--|--------------|--------------|--------------|--------------|--------------------------|--------------|------------------------|--------------|
| | | | % Alui | minum | 10 | oluble | % Insoluble Zirconium | | % Total Rare Earths | |
| | Procedure | | Run No. 1 | Run No. 2 | Run No. 1 | Run No. 2 | Run No. 1 | Run No. 2 | Run No. 1 | Run No. 2 |
| 1. | Melt charge, heat 1400 F, stir, hold 15 min | | 0.0032 | 0.0025 | 0.30 | 0.17 | 0.04 | 0.05 | 3.34 | 3.54 |
| 2. | At 1400 F add 0.002% Al, hold 15 min | | 0.0032 | 0.0028 | 0.28 | 0.14 | 0.03 | 0.05 | | 3.39 |
| 3. | At 1400 F add 0.005% Al, total, hold 15 min | | 0.0036 | 0.0038 | 0.25 | 0.11 | 0.02 | 0.04 | 3.31 | 3.37 |
| 1. | At 1400 F add 0.010% Al, total, hold 15 min | | 0.0038 | 0.0048 | 0.21 | 0.09 | 0.03 | 0.03 | | 3.35 |
| 5. | At 1400 F add 0.020% Al, total, hold 15 min | | 0.0057 | 0.0074 | 0.16 | 0.09 | 0.06 | 0.03 | 3.37 | 3.37 |
| 6. | At 1400 F add 0.040% Al, total, hold 15 min | | 0.0083 | 0.0155 | 0.12 | 0.03 | 0.04 | 0.04 | | 3.44 |
| 7. | At 1400 F add 0.080% Al, total, hold 15 min | | 0.022 | 0.042 | 0.06 | 0.03 | 0.02 | 0.02 | 3.31 | 3.31 |
| 3. | At 1400 F add 0.16% Al, total, hold 15 min | | 0.086 | 0.12 | 0.03 | 0.01 | 0.01 | 0.01 | | 3.27 |
| 9. | At 1400 F add 0.32% Al, total, hold 15 min | | 0.12 | 0.20 | 0.01 | 0.01 | 0.01 | 0.01 | 3.46 | 3.28 |

TABLE 2-EFFECT OF STIRRING AND HOLDING ON ANALYSIS OF EK30 ALLOY

| | | Ch | emical Analys | is |
|-----|-------------------------|------------------------|--------------------------|------------------------|
| | Procedure | % Soluble Zirconium | % Insoluble Zirconium | % Total Rare Earths |
| 1. | Melt charge, heat 1400 | | | |
| | F and stir, hold 15 min | 0.30 | 0.04 | 3.50 |
| 2. | At 1400 F stir, | | | |
| | hold 15 min | 0.29 | 0.04 | 3.47 |
| 3. | At 1400 F stir, | | | |
| | hold 15 min | 0.28 | 0.04 | 3.43 |
| 4. | At 1400 F stir, | | | |
| | hold 15 min | 0.28 | 0.04 | 3.47 |
| 5. | At 1400 F stir, | | | |
| | hold 15 min | 0.28 | 0.04 | -3.53 |
| 6. | At 1400 F stir. | | | |
| | hold 15 min | 0.27 | 0.04 | 3.48 |
| 7. | At 1400 F stir, | | | |
| *** | hold 15 min | 0.27 | 0.04 | 3.48 |
| 8. | At 1400 F stir. | | | |
| - | hold 15 min | 0.26 | 0.03 | 3.45 |
| 9. | At 1400 F stir. | - | - | |
| | hold 15 min | 0.26 | 0.03 | 3.47 |

num is added.* After 0.16 per cent aluminum has been added, the zirconium is practically nil, while the aluminum has gradually increased. Very little, if any, change can be seen in the rare earth analysis.

As a blank for the above test, a third melt was made repeating the same steps as above except that no aluminum addition was made. While there is a slight decrease in zirconium content (0.34 per cent total to 0.29 per cent total) on this melt, this loss is only a small portion of the loss obtained when adding aluminum to molten EK30 alloy. The data are presented in Table 2.

The effect on the analysis of remelting EK30 alloy eight times is depicted in Table 3. Duplicate tests were run to check this effect. The original charge on each test consisted of 55 lb of KO ingot and 55 lb of E7 ingot. The ingot was melted in standard mild steel crucibles, heated to 1400 F and stirred thoroughly with No. 220 flux to refine the melt. After holding at 1400 F for 30 min the metal was heated to 1540 F, sampled, and cast into pigs. Heating to 1540 F was to

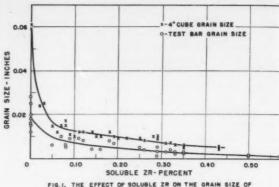


FIG. 1, THE EFFECT OF SOLUBLE ZR ON THE GRAIN SIZE OF Mg-3% RARE EARTH ALLOYS.

simulate actual conditions as they may exist in the production foundry. The pigs were remelted eight times with no virgin ingot being added after the first melt. The data from two series of melts show a 9 per cent loss in rare earth content. Although real, this loss is not so great that it would become a problem as long as some virgin ingot is used in the charge. The reduction of the zirconium upon remelting a number of times amounts to 80 per cent of the original zirconium content and calls for special measures to ensure adequate zirconium in the final casting. These special measures may be either one of two possibilities: (1) the addition of about 20 per cent virgin ingot to each melt using E10 and KO in proper proportions, or (2) the use of some form of metallic zirconium as a sweetener.

The iron content of a magnesium-zirconium melt increases whenever the melt is held at relatively high temperatures for a length of time in contact with iron or steel. When the temperature of the melt is lowered, a form of iron-zirconium compound will settle out resulting in both a low iron content and a reduced zirconium content. It is recommended, as a result of this fact, that foundry scrap containing screens and steel wool be remelted by adding it to a molten heel of alloy at a temperature of 1300 to 1400 F. The screens and steel wool should be removed from the crucible as soon as the EK30 alloy has melted off them. No difficulty is encountered in keeping within the analysis limits when operating in this manner with melts ranging in size up to 1500 lb. Mild steel

TABLE 3-EFFECT OF REMELTING ON ANALYSIS OF EK30 ALLOY

| | | | Chemi | cal Analysis | | |
|---|-----------|------------------|-----------|--------------|------------------------|-----------|
| | / % | Soluble irconium | 10 | nsoluble | % Total Rare Earths | |
| Procedure | Run No. 1 | Run No. 2 | Run No. 1 | Run No. 2 | Run No. 1 | Run No. 2 |
| 1. Melt charge, heat 1400 F, stir, hold 30 min, | | | | | | - |
| heat 1540 F, cast ingots | 0.26 | 0.15 | 0.04 | 0.02 | 3.47 | 3.31 |
| 2. Remelt ingot from 1, repeat procedure in 1 | 0.22 | | 0.03 | | 3.47 | |
| 3. Remelt ingot from 2, repeat procedure in 1 | 0.17 | 0.07 | 0.03 | 0.01 | 3.30 | 3.25 |
| 4. Remelt ingot from 3, repeat procedure in 1 | 0.15 | | 0.03 | | 3.29 | |
| 5. Remelt ingot from 4, repeat procedure in 1 | 0.12 | 0.03 | 0.03 | 0.01 | 3.16 | 3.19 |
| 6. Remelt ingot from 5, repeat procedure in 1 | 0.09 | | 0.02 | | 3.20 | |
| 7. Remelt ingot from 6, repeat procedure in 1 | 0.12 | 0.01 | 0.01 | 0.01 | 3.21 | 3.15 |
| 8. Remelt ingot from 7, repeat procedure in 1 | 0.06 | | 0.01 | | 3.21 | |
| 9. Remelt ingot from 8, repeat procedure in 1 | 0.05 | 0.01 | 0.01 | 0.01 | 3.14 | 3.00 |

^{*} The terms soluble and insoluble refer to those portions of the contained zirconium in the alloy which are soluble or insoluble in dilute hydrochloric acid.

crucibles are used in these melts with no serious iron pickup or loss in zirconium.

Occasionally, a melt becomes accidentally contaminated with one of the zirconium precipitating agents and the zirconium in the melt is reduced to below the specified minimum. When this occurs, there are two techniques which may be employed to remedy the situation. The safer process, but perhaps the less desirable from a production standpoint, is to convert the melt to E7 or E10 alloy and re-use it as one of the ingredients of future melts. The more convenient solution, production-wise, is to add a metallic zirconium sweetener to the melt. Such high zirconium hardeners are commercially available, and they possess the advantage of not adding any fluid flux or magnesium chloride to the casting crucible. They are expensive, however, and not efficient as alloying agents.

Zirconium, in magnesium alloys, is an excellent grain refiner, and it thereby improves both the mechanical properties and the castability of a foundry alloy. Figure 1 presents a graph showing the influence of soluble zirconium on the grain size of magnesium-3 per cent rare earth alloys. This figure shows the grain size measured in the center of a 4-in. cube which

is poured for the purpose of obtaining grain sizes representative of those in heavy or slowly cooled castings. Also shown is the grain size from the reduced section of a standard one-half inch diameter tensile bar. The tremendous grain refining effect of the first 0.1 per cent soluble zirconium and the rather slow rate of continued refinement beyond 0.1 per cent zirconium is apparent. This initial refinement is important to both the foundry and the consumer because it imparts sufficient ductility to the part to make it castable and usable at room temperature. The additional grain refinement with increased zirconium content has not appeared to yield any improvement in castability.

Casting

Good quality EK30 alloy castings have been produced in the foundry using the same molding and core sands, sprays, chills, and pattern rigging which have been used successfully in producing magnesium-aluminum alloy castings. There have been cases where slight modifications in pattern rigging have been necessary to cast a part successfully in EK30 alloy. No other changes in the handling of the molds have been made. In general, the ratio of metal cast to metal shipped on

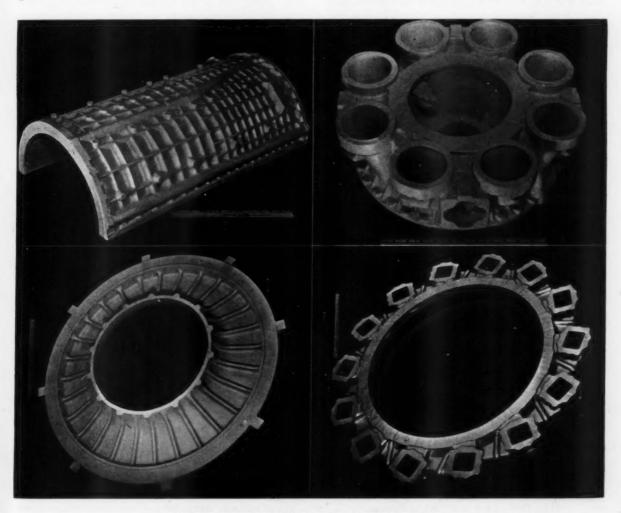


Fig. 2-A group of EK30 alloy aircraft engine castings.

an individual casting in EK30 alloy is the same as in magnesium-aluminum alloys.

Complete dimensional checks of experimental EK30 alloy castings disclosed that they were shorter in length and breadth than castings made from AZ92 alloy produced from the same pattern equipment. They had a shrinkage of approximately 25 per cent more than the AZ92 alloy castings. The linear shrinkage as determined on laboratory tests of AZ92 alloy and three magnesium-rare earth base compositions is listed in Table 4. This test, described by Busk and Marande,11 is made by measuring the linear shrinkage of a 1 in. x 1 in. x 48 in. bar cast in a sand mold. This linear shrinkage test bar is one which has no restraint on it so that it has complete freedom to shrink. Data from these tests indicate about 30 per cent increase in shrinkage for EK30 alloy as compared with AZ92 alloy. Apparently a production EK30 casting made in a sand mold is restrained to an extent that its shrinkage is only about 25 per cent greater than with AZ92 alloy.

The magnesium-ra e earth-zirconium alloys, when molten, tend to oxidize in air more rapidly than the other commercial magnesium casting alloys. This fac-

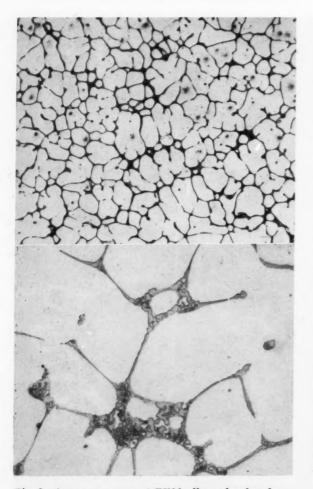


Fig. 3—As-cast structure of EK30 alloy; glycol etchant.

Mag. 100x at top and 500x at bottom.

TABLE 4-COMPARATIVE LINEAR SHRINKAGE AZ92 VS MG-RARE EARTH-BASE ALLOYS

| Composition | Shrinkage* (in. per 4 ft) | % Increase over AZ92 Alloy |
|-------------------------------|---------------------------|-------------------------------|
| AZ92 | 0.52 | _ |
| Mg + 3 Rare Earth | 0.72 | 38 |
| Mg + 3 Rare Earth + 0.20 Zr | 0.69 | 32 |
| Mg + 3 Rare Earth $+ 0.37$ Zr | 0.66 | 27 |
| * Measured on a 48 in. | oar cast in san | d mold. |

tor requires that these alloys be handled in a somewhat more careful manner than would be the case with alloys of the magnesium-aluminum-zinc system. The molten alloy in the crucible must be well protected with Dow No. 181 agent during the pouring operation, and attention must be given in the sand mold to those factors which reduce the turbulence of the molten stream. When the alloy is not properly handled in this respect, a form of pitting on the cope surfaces of the casting is frequently encountered. In addition to these pits, gross misruns may also occur when sufficient screening is not provided to remove oxide skins. While cope pitting and entrapped skins

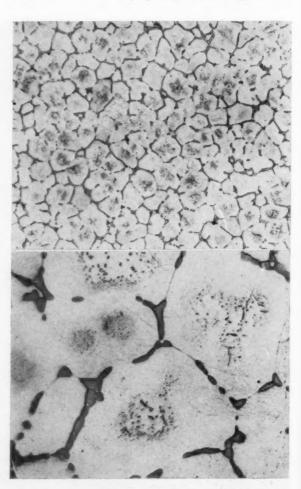


Fig. 4—Heat treated and aged structure of EK30 alloy; glycol etchant. Mag. 100x at top, and 500x, bottom.

may be obtained in other magnesium alloys such as AZ92 or AZ63 alloys, it has been found to require less care to eliminate them in those alloys than it does in alloys of the magnesium-rare earth-zirconium system.

Microporosity in EK30 alloy, though occasionally encountered, generally does not cause difficulty in sand foundry operations. The amount of microporosity is approximately equal to AZ92 alloy and is, of course, much less than that which occurs in AZ63 alloy. However, the magnesium-rare earth-zirconium alloys exhibit a greater tendency toward surface shrinks and shrinkage cracks than commercial magnesium-aluminum-zinc alloys. These shrinks and cracks are particularly evident in heavy sections which are isolated from risers by intervening thin sections. The cracks usually occur at the junction of these heavy and thin sections.

Alloys in the rare earth system can be readily and effectively heat treated. The improper heat treatment, however, of alloys in this system may cause the formation of pits on the surface of the castings. The pits range in size from a pin point to about ½6 in. in diameter and are particularly evident after an acid dip. This phenomenon is the result of oxidation of the segregated Mg₉Ce compound in the casting and occurs when an insufficient concentration of SO₂ gas is present in the furnace. No troubles have been en-

countered when the concentration of SO_2 is one per cent or greater.

A series of pictures (Fig. 2) best describe the type of aircraft castings which have been produced in EK30 alloy. It can be seen that they range from rather simple to quite complex castings. In Fig. 3 and 4 the microstructures of EK30 alloy are shown for both the as cast and heat treated and aged conditions.

A short discussion comparing the castability features of various magnesium-rare earth base alloys is now desirable. There are two factors in this system which contribute to poor castability: the coarse grain structure and the rather large amount of embrittling second phase material. It is through attempts to eliminate these factors which ultimately led to the adoption of EK30 alloy for elevated temperature applications.

Simply designed engine parts can be produced in magnesium-6 per cent earth-1 to 2 per cent manganese (EM61 and EM62) alloys. Engine tests substantiate results obtained from elevated temperature tensile and creep properties as determined on separately cast test bars of these compositions. The addition of a small amount of zirconium to these alloys resulting in a grain refinement from about 0.15 in. to 0.015 in. average grain diameter in small sections did not improve the castability sufficiently.

TABLE 5-CHEMICAL ANALYSIS, HEAT TREATMENT AND GRAIN SIZE OF VARIOUS MAGNESIUM ALLOYS

| | | Chemical A | nalysis | | | Grain |
|----------------------|----------------------|------------------------|-----------|---------------|---|-----------------|
| Alloy Designation | % Total Zirconium | % Total Rare Earths | % Zinc | % Aluminum | Heat Treatment | Size (Inches |
| EK30 | 0.34 | 3.23 | _ | _ | HTA:16 hr at 1050 F, cool to room | 0.003- |
| | | | | | temperature, 16 hr at 400 F | 0.005 |
| EK31 | 0.59 | 3.21 | _ | | HTA:16 hr at 1050 F, cool to room temperature, 16 hr at 400 F | 0.001 |
| EK60 | 0.37 | 6.12 | _ | _ | HTA:16 hr at 1050 F, cool to room temperature, 16 hr at 400 F | 0.002 |
| EZ33 | 0.54 | 2.93 | 3.09 | games. | ACA:12 hr at 350 F | 0.002 |
| EZ33 | 0.54 | 2.42 | 3.05 | - | HTA:16 hr at 950 F, cool to room temperature, 16 hr at 400 F | 0.004- 0.006 |
| ZRE1* | 0.6 | 2.5 | 2.5 - 3 | _ | Low temperature treatment | - |
| E3 | - | 3.23 | _ | _ | HTA:16 hr at 1050 F, cool to room | 0.020- |
| | | | | | temperature, 16 hr at 400 F | 0.028 |
| AZ92 | _ | _ | 2.0 | 9.5 | HTA:2 hr at 670-770 F, 18 hr at 770 F, cool to room temperature, 16 hr at 350 F | 0.003 |
| * Data | a obtained fro | m a published | brochure | | • | |

TABLE 6-ROOM AND ELEVATED TEMPERATURE TENSILE PROPERTIES OF VARIOUS MAGNESIUM ALLOYS

| | | | | | | | Testing | Temp | erature | | | | | | |
|----------------------|-------|-------|--------|-------|-------|--------|---------|-------|---------|-------|-------|--------|--|-------|-------|
| | | 70 F | - | | 300 F | 7 | | 400 F | , | | 500 F | , | The state of the s | 600 F | |
| Alloy Designation | | | % . | | | % * | | | % * | | | % * | | | % . |
| and Condition | T.S.X | Y.S.X | Elong. | T.S.X | Y.S.X | Elong. | T.S.x | Y.S.X | Elong. | T.S.X | Y.S.X | Elong. | T.S.X | Y.S.X | Elong |
| ЕК30-НТА | 22.9 | 16.5 | 2.4 | 20.8 | 14.7 | 7.7 | 20.0 | 14.0 | 13.1 | 17.5 | 12.1 | 29.7 | 11.8 | 7.7 | 69.6 |
| EK31-HTA | 26.5 | 19.8 | 2.9 | 22.0 | 17.0 | 11.2 | 21.4 | 16.5 | 13.0 | 18.6 | 13.4 | 21.5 | 13.3 | 9.2 | 52.0 |
| EK60-HTA | 23.7 | 19.7 | 0.5 | 21.0 | 18.0 | 1.0 | 21.0 | 17.0 | 2.0 | 19.2 | 14.0 | 4.8 | 14.1 | 9.0 | 38.0 |
| EZ33-ACA | 17.4 | 13.4 | 1.8 | 22.0 | 10.0 | 11.8 | 20.0 | 9.0 | 19.0 | 17.0 | 9.0 | 35.0 | | | |
| EZ33-HTA | 19.6 | 12.3 | 1.5 | 21.3 | 11.3 | 15.0 | 18.7 | 10.7 | 16.2 | 13.7 | 9.6 | 44.2 | 8.9 | 6.2 | 64.0 |
| ZRE1** | 20.3 | 12.3 | 3-6 | | | | 20.1 | 10.5 | 20-25 | | | | | | |
| AZ92-HTA | 20.5 | 12.4 | 1.5 | 17.9 | 12.9 | 7.0 | 16.8 | 11.9 | 4.7 | 17.9 | 11.3 | 11.7 | 13.5 | 8.8 | 24.2 |
| Е3-НТА | 41.9 | 26.0 | 2.0 | 28.2 | 18.0 | 37.0 | 16.9 | 10.9 | 36.0 | 12.6 | 8.5 | 52.0 | 7.6 | 4.7 | 108.0 |

• Per cent elongation in 2 in.

* 1000 psi

Note: All elevated temperature tensile bars were held 10 min at temperature prior to testing.

Elimination of manganese from this system reduces the grain size still further and somewhat improves its ductility at room temperature without seriously affecting its creep characteristics. Although castability difficulties are great, a number of aircraft engine parts were cast in this EK60 alloy (magnesium-6 per cent rare earths-0.1 to 0.4 per cent zirconium. These parts have apparently performed satisfactorily when sub-

jected to engine tests.

A reduction of the rare earth content from 6 per cent to 3 per cent decreased the amount of second phase material observed in this magnesium-3 per cent rare earth alloy. The combination of fine grain and lowered amount of second phase material results in a composition which has the foundry characteristics necessary to handle it economically in production. EK30 and EK31 alloys (magnesium-2.0 per cent to 4.0 per cent rare earth-0.55 per cent minimum zirconium) can be handled similarly as far as castability is concerned. The addition of 3 per cent zinc to both EK30 and EK31 has not increased the castability of either alloy as evaluated in our production casting tests.

Property Evaluation

The alloy designation, chemical analysis heat treatment, and grain size of the alloys whose properties are presented for comparative purposes are listed in Table

TABLE 7-CREEP LIMIT OF VARIOUS MAGNESIUM ALLOYS AT ELEVATED TEMPERATURES

| Alloy | Testing | | Creep Limit | • |
|------------------------------|---------|-------------------------|-------------------------|------------------------|
| Designation and Condition | Temp., | 0.1% Creep Extension | 0.2% Total Extension | 0.5% Tota Extension |
| ЕК30-НТА | 400 | 7.9 | 7.2 | 10.1 |
| | 500 | 2.5 | 2.5 | 3.6 |
| | 600 | 1.3 | 1.5 | 1.7 |
| EK31-HTA | 400 | 8.8 | 8.0 | 11.2 |
| | 500 | 3.4 | 3.6 | 4.4 |
| | 600 | 1.3 | 1.5 | 1.7 |
| EK60-HTA | 400 | 8.8 | 8.0 | 11.0 |
| | 500 | 3.0 | 3.3 | 4.2 |
| | 600 | 1.1 | 1.3 | 1.7 |
| EZ33-ACA | 400 | 8.4 | 8.0 | 9.8 |
| | 500 | 3.7 | 3.9 | 5.0 |
| | 600 | 1.2 | 1.4 | 1.8 |
| EZ33-HTA | 400 | 8.0 | 7.7 | 9.5 |
| | 500 | 3.0 | 3.5 | 4.7 |
| | 600 | 1.1 | 1.4 | 1.9 |
| ZRE1* | 400 | 9.4 | 7.8 | 10.0 |
| E3-HTA | 400 | 10.0 | 8.4 | 10.2 |
| | 500 | 4.7 | 4.7 | 6.2 |
| | 600 | 2.2 | 2.5 | 2.8 |
| AZ92-HTA | 300 | 3.3 | 3.5 | 6.2 |
| | 400 | 0.9 | 1.3 | - |
| | 500 | 0.4 | 0.6 | 1.1 |
| * 100-hr test, 100 | 00 psi | | | |

TABLE 8-AVERAGE TENSILE PROPERTIES OF BARS CUT FROM EK30 ALLOY CASTINGS, HEAT TREATED AND AGED, THEN MACHINED

| Chemical Analysis | | | | | Average | Room Temperature | | | |
|----------------------|-----------------------|-------------------|--------|------|------------------|-------------------|------------------|---------------|--|
| % Total Zirconium | % Total Rare Earth | Number of Bars | Round | Flat | Grain Size (In.) | T.S.* | Tensile Propert | ies % Elon.• | |
| 0.35 | 3.01 | 2 | _ | x | ,0.006 | 19.8 | 15.2 | 0.7 | |
| 0.55 | 3.01 | 2 | x | * | 0.007 | 19.2 | 15.4 | 0.7 | |
| | | 2 | x | | 0.006 | 17.3 | 13.6 | 0.2 | |
| 0.35 | 3.01 | 4 | x | | 0.004 | 18.3 | 15.4 | 0.4 | |
| | | 4 | | x | 0.003 | 20.1 | 16.4 | 1.0 | |
| | | 3 | | x | 0.004 | 19.5 | 15.5 | 1.0 | |
| 0.20 | 2.96 | 4 | x | | 0.012 | 16.0 | 13.8 | 0.4 | |
| | | 4 | x | | 0.008 | 20.7 | 14.4 | 0.8 | |
| 0.20 | 2.91 | 3 | x | | 0.009 | 18.9 | 13.4 | 1.1 | |
| | | 3 | x | | 0.012 | 17.2 | 14.2 | 0.7 | |
| | | 3 | x | | 0.012 | 16.9 | 14.0 | 0.5 | |
| 0.16 | 2.96 | 2 | x | | 0.012 | 17.3 | 14.3 | 0.2 | |
| | | 2 | x | | 0.014 | 16.8 | 14.4 | 0.5 | |
| | | 2 | x | | 0.010 | 17.4 | 14.3 | 0.5 | |
| 0.30 | 2.90 | 2** | x | | 0.005 | 19.8 | 14.7 | 1.2 | |
| Per cent elon | gation in 2 in. | | × 1000 | psi | | • • Bars machined | from 11/8 in. di | am x 11 in. b | |

Table 9-Comparative Tensile Properties of Bars Cut from Welded and Non-Welded Panels VS TENSILE PROPERTIES OF SEPARATELY CAST TEST BARS

EK30-HTA Condition

| Chemical Analysis % Total % Total | | | | Number | Average Grain Size of Base Metal (In.) | Room Temperature Tensile Properties | | |
|------------------------------------|---------------|------------|---------------------------|--------|--|--|-------|----------|
| Zirconium | Rare Earths | Condition | Type of Test Bar of Bars | | | T.S.x | Y.S.* | % Elong. |
| 0.18 | 2.73 | Welded** | Machined from Panel | 6 | 0.007 | 20.1 | 15.4 | 1.7 |
| | | Not Welded | Separately cast test bars | 2 | 0.004 | 24.7 | 15.2 | 4.0 |
| 0.34 | 3.67 | Welded** | Machined from Panel | 10 | 0.003 | 22.2 | 16.9 | 1.1 |
| | | Not Welded | Machined from Panel | 10 | 0.003 | 21.9 | 16.8 | 0.9 |
| • Per cent | Elongation in | 2 in. | * 1000 psi | | ** EK30 | Welding 1 | Rod | |

x 120-hr test, 1000 psi obtained from a published brochure.

5. The comparison method for determining grain size as established by P. F. George¹² is used on these alloys. The room and elevated temperature tensile properties for these alloys are reported in Table 6 while the creep resistance is recorded in Table 7.

These tables indicate that EK30 alloy has good tensile properties and resistance to creep. All of the magnesium - rare earth - zirconium alloys, including those containing zinc, are essentially equal in these properties; and they are outstanding in their improvement in creep strength over any of the alloys in the magnesium-aluminum-zinc system. The EK30 alloy can be used in the as cast and aged condition if desired. In this condition its room temperature tensile properties are slightly inferior to those obtained in the heat treated and aged condition. In order to obtain the maximum tensile properties the alloy should be heat treated at about 1050 F before aging. Since all of these rare earth-containing alloys are comparable in tensile and creep properties, the choice between them becomes one of economics. The Dow foundry has found that EK30 alloy can be handled more economically than these other alloys and at production rates comparable to the other commercial magnesium alloys.

A number of representative EK30 castings were sectioned, the pieces heat treated and aged, and then round or flat test bars machined from the pieces. In Table 8 is listed the analysis, grain size, and average tensile properties of these bars tested at room temperature. The yield strengths are comparable to those found in separately cast 1/2-in. diam test bars of the same composition. However, the ductility of the bars is reduced appreciably while the tensile strength is somewhat lowered. Also included in Table 8 are the tensile properties of test bars in the heat treated and aged condition which were machined from cast bars 11/2 in. in diameter by 11 in. in length. Again the properties are somewhat below those of separately cast 1/2-in. diam bars.

EK30 alloy castings can be satisfactorily welded for salvage purposes using helium or argon arc techniques in the same manner as AZ92 and AZ63 alloys. The room temperature tensile properties of EK30 alloy panels welded with EK30 alloy rod are shown in Table 9. The weld metal is uniformly fine grained, no evidence of cracking has been noted, and, as the table illustrates, satisfactory properties can be obtained.

In regard to chemical treatment, the magnesiumrare earth-zirconium alloys behave in a manner similar to M1 alloy (magnesium-1.5 manganese). The Dow No. 1 Chrome-Pickle (AN-M-12 Type I), which is generally used to protect a casting during shipment and storage before machining, may be applied to EK30 and related alloys in the same manner as is recommended for AZ92 alloy. The Dow No. 7 treatment (AN-M-12 Type III), which is the final coating that is generally used prior to painting, cannot be used on these alloys. In this respect, they are similar to M1 alloy. In its place, either the Dow No. 9 galvanic anodize (AN-M-12 Type IV) or the Dow No. 10 sealed Chrome-Pickle (AN-M-12 Type II) may be used.

Summary

EK30 alloy is comparable in tensile and creep properties to the other alloys in the rare earth class, and is far superior to the magnesium-aluminum-zinc systems at elevated temperatures. This conclusion is drawn from data presented in this paper comparing these properties in AZ92 with the properties of magnesium-3 per cent to 6 per cent rare earth-0.0 per cent to 3.0 per cent zinc-0.0 per cent to 0.6 per cent zirconium.

The foundry characteristics of EK30 alloy, as set forth in this paper, show this alloy to be about equal in castability to AZ92 and AZ63 alloys. In addition the experience which has been obtained in production, using the alloying procedures discussed, indicate that this alloy can be handled in a foundry more advantageously than can the higher rare earth- or higher zirconium-containing alloys.

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DISCUSSION

Chairman: R. T. Wood, American Magnesium Corp., Cleve-

Co-Chairman: J. C. McDonald, The Dow Chemical Co., Midland, Mich.

J. W. MEIER (Written Discussion): 1 In view of the recent progress in the field of jet-propelled engines, the development of magnesium alloys suitable for elevated temperature service became very important and urgent. The authors are to be congratulated for their interesting paper and the presentation of valuable data on one of the more promising alloys for elevated temperature applications.

Experimental work on similar lines was conducted, in the last few years, at the Canadian Bureau of Mines and we presented recently some of our results.* In general our results are very similar to those reported by the authors but we cannot agree

with the conclusions reached in this paper.

The choice of alloy EK30 as the most favorable magnesium casting alloy for elevated temperature service is based by the authors on two characteristics of this alloy as compared with other rare earth containing magnesium-base alloys, that is, its higher room-temperature properties and its superior castability.

The room-temperature properties are related to the grain size. The authors investigated the effect of zirconium effect on the castability of the alloy. We found that higher zirconium contents improve very markedly the tensile properties at room tem-

Ont., Canada.

^{*} J. W. Meier and M. W. Martison—"Magnesium Casting Alloys for Elevated Temperature Service"—presented at the Annual Meeting, Canadian Institute of Mining and Metallurgy, Toronto, April 18, 1950. ¹ Head, Nonferrous Metals Div., Canadian Bureau of Mines, Ottawa,

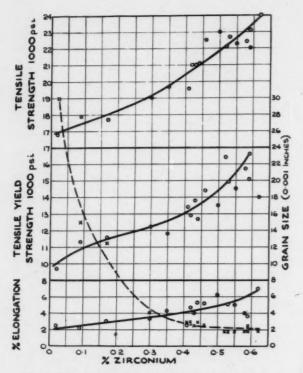


Fig. A-Effect of Zirconium Content on Room Temperature Properties of Mg-3% Ce Alloys Test Bars Cast-to-Shape All Results in the "As-Cast" Condition

perature both in the as-cast (see Fig. A) and especially in the heat treated condition. The authors show also, in Tables 6 and 7, that the elevated temperature properties of alloy EK31 (with higher zirconium content) are superior to those reported for alloy EK30.

As reported earlier by Murphy and Payne, the originators of magnesium casting alloys MCZ (EK31), and confirmed in our investigation, there is no difficulty in the preparation of higher zirconium containing alloys, in the range of 0.5 to 0.6 per cent Zr. I would like to ask the authors to explain why they prefer alloy EK30 contrary to their own evidence which shows superior properties for alloy EK31.

One of the more important foundry characteristics of any casting alloy is the total linear shrinkage (or pattern shrinkage). Our investigation showed that alloys EK30 and EK31 have a much higher shrinkage factor than other commercial magnesium casting alloys. Average linear shrinkage values, as measured on 48-in. experimental bars similar to those used by the authors, are listed in Table A. It is known that on larger commercial castings slightly higher shrinkage factors are common.

TABLE A-LINEAR SHRINKAGE FACTORS

| Alloy | Shrinkage | | | |
|------------------|-----------------|-----|----|---|
| AZ63, AZ80, ZK61 | 0.140-0.160 in. | per | ft | |
| ZEK331, EM61 | 0.170-0.185 in. | per | ft | * |
| EK30, EK31 | 0.190-0.200 in. | per | ft | |

As I mentioned, these figures represent total or pattern shrinkage and not the "shrinkage after solidification of the castings" as reported by the authors in Table 4. The higher shrinkage factor of the EK alloys is an important consideration whenever patterns are to be used interchangeably with other alloys.

Another point is the effect of rare earth content. Table B shows some of our results of creep-rupture tests at 200 C (400 F) for alloys with 2 to 5 per cent R.E. and approximately 0.5 per

cent Zr, in the as-cast and stabilized condition. The values shown indicate that higher rare earth contents improve decidedly the elevated temperature properties of these alloys.

TABLE B-RESULTS OF CREEP-RUPTURE TESTS AT 200 C (400 F) OF SOME MAGNESIUM-RARE EARTH-ZIRCONIUM ALLOYS (All tests in the as-cast and stabilized condition)

| Alloy | Melt | Composition, % Size Stress, in 1,000 psi, to Rupture | | | | | |
|-------|------|--|------|-----------|--------|--------|---------|
| | No. | R.E. | Zr | 0.001 in. | 100 hr | 500 hr | 1000 hr |
| EK21 | 1235 | 1.85 | 0.56 | 1.9 | 12.1 | 9.7 | 8.9 |
| EK31 | 1236 | 3.16 | 0.55 | 1.8 | 12.8 | 10.6 | 9.8 |
| EK41 | 1237 | 3.94 | 0.48 | 1.8 | 13.2 | 11.0 | 10.2 |
| EK51 | 1239 | 4.92 | 0.50 | 2.0 | 14.3 | 11.6 | 10.6 |

The authors show all their results in the "heat treated and aged" (HTA) condition. It is known from work on other magnesium-base alloys that in most cases the best creep characteristics at elevated temperatures (especially in the higher range) are being obtained in the "as cast and stabilized" (ACS) condition. Could the authors show the difference in creep properties for the EK alloys in these two conditions? If the HTA condition was chosen because of the increased room temperature strength I would like to add that tensile properties shown in Table 6 for alloy EK30-HTA may be obtained in alloy EK31-AC without any heat treatment.

There is another point regarding the use of the HTA condition. Properties at temperatures at or above the aging temperature should be tested after a stabilization treatment at a temperature somewnat higher than the testing temperature, otherwise

the results are of little or no practical value.

Research on magnesium-base alloys for elevated temperature service is being conducted mainly, if not exclusively, for the aircraft industry where the highest strength-to-weight ratio is the most essential characteristic for any material to be used. Therefore, the service characteristics of the alloy are, from the users point of view, much more important than some slight advantage in foundry handling. And this is why we believe that alloy EK31 should be used in preference to the lower-strength alloy EK30.

MR. MEIER'S RESEARCH CONCLUSIONS

To sum up our work on these alloys I will quote the conclusions reached in our paper to the CIMM:

1. Elevated temperature properties, especially the creep strength, are markedly improved with increasing rare earth contents and additions of manganese. Unfortunately, alloys with higher R.E. contents show very poor foundry characteristics and cannot be, therefore commercially used. Manganese additions increase the creep strength but cause considerable grain coarsening and hot shortness of the alloys.

2. Zirconium additions provide an extremely fine grain structure and higher room-temperature properties; zinc additions improve also the foundry characteristics and cause further increase in room temperature strength. Unfortunately, both the Mg-R.E.-Zr and the Mg-R.E.-Zn-Zr alloys show lower creep strength above 400 F than the Mg-R.E. and Mg-R.E.-Mn alloys.

3. Further research should be directed to improve the foundry characteristics of the Mg-R.E. or Mg-R.E.-Mn alloys, by alloying additions or development of special castings methods, or to develop new alloys of superior elevated temperature properties.

4. Until this can be achieved the following magnesium casting alloys should be recommended for applications where the highest possible strength-to-weight ratio is essential:

(a) for temperatures up to 250 or 300 F—high-strength Mg-Zn-Zr alloys (e.g. alloy ZK61);

(b) for temperatures from 250 to 400 F-Mg-R.E.-Zr or Mg-R.E.-Zn-Zr alloys (e.g. alloys EK31 or EZK331), and

(c) for temperatures above 400 F (or maybe 450 F)—Mg-R.E. or Mg-R.E.-Mn alloys, with or without further alloying additions (e.g. E6 or EM61).

K. E. Nelson (Authors' Reply to Mr. Meier): I want to thank Mr. Meier for his discussion on our paper. I am sure that this type of exchange of ideas will be beneficial to everyone in the industry. There are some differences in opinion brought forth by Mr. Meier which I will try to modify.

The first question asked is why we prefer to use EK30 rather than EK31 alloy.

Our choice of composition is based upon several factors including ease of handling in the foundry, and ultimate cost to the consumer as well as the mechanical properties of the alloy. We believe that to all practical purposes there is no significant difference in tensile or creep properties between any of several alloys containing magnesium, Mischmetal, zirconium and with or without zinc. All of these alloys are vastly superior to the older Mg-Al base alloys for elevated temperature service. However, we have found that an appreciable difference exists between these alloys in some other respects. As far as ease of handling in a foundry is concerned we think that EK30 alloy has a definite advantage. This composition can be used with tilter practice and then poured into several crucibles prior to casting with no need to be concerned about alloy composition. In this respect, EK30 alloy can be handled in a manner very similar to the other commercial Mg alloys containing aluminum and zinc as the principle alloying ingredients. The higher Zr containing alloys require much more careful handling to maintain the zirconium content at its required level.

The cost item is also one of which varies appreciably between various alloys of this class. Here we feel also that EK30 has a definite advantage in any foundry. This is based not only on the fact that this alloy is easier to handle in a foundry but also on a cost of materials basis. The amount of zirconium required is certainly less to maintain it at a level of 0.25 per cent than it would be to maintain it at any higher level. The cost of the zirconium is an appreciable fraction of the total cost of the alloy to the customer. When the costs and properties are considered we feel certain that both the producer and the consumer are justified in using EK30 alloy for high temperature applications.

Mr. Meier's second question is in regard to shrinkage of the various Mg-R.E.-Zr alloys. Mr. Meier shows that the alloy ZRE1 (British designation) or EZ33 (ASTM designation) is more similar to AZ80 and AZ63 alloys than is EK30 alloy when

shrinkage in a mold was concerned.

We have not measured the shrinkage of EZ33 alloy and therefore cannot compare its shrinkage with EK30 alloy. We have, however, poured EZ33 and EK30 using the same pattern equipment. The castings poured were sent to a customer for test purposes. No mention of dimensional differences was made by the customer. It can be concluded that if there were differences they were minor.

We have stated that, on our test using the 48-in. bar to measure shrinkage in a mold, the EK30 alloy shrinks 25-30 per cent more than AZ92 alloy. In aircraft engine castings the shrinkage has ranged up to 50 per cent more. This variation was due to the differences in restraint within the mold caused by various cores and casting design. Our data, as does also Mr. Meier's, represents total or pattern shrinkage.

Mr. Meier also asked why we used EK30 in the heat treated and aged condition rather than in the as-cast and aged condition. He felt that if we are interested in good creep resistance with increased room temperature tensile properties, we should

consider EK31 in the as-cast and aged condition.

This is a very good comment as it entails many considerations. It is a fact that the room and elevated temperature tensile properties are improved by a heat treatment prior to aging. The improvement in room temperature elongation as a result of the solution heat treatment and aging results in an alloy which facilitates the handling of the casting by the consumer during machining and assembling operations. The higher strength ob-

tained insure better service during and after assembly. The temperature of this treatment is, however, quite high. Our reason for maintaining the low Zr level rather than the higher level has been explained in this discussion. In our foundry we believe it to be more economical to heat treat EK30 alloy rather than to produce EK31 alloy. The tensile and creep properties in Table C are submitted for comparative purposes.

L. W. EASTWOOD (Written Discussion): ² Messrs. Nelson and

L. W. Eastwoop (Written Discussion): Messrs. Nelson and Strieter are to be complimented on preparing a very interesting and timely paper on magnesium alloys for elevated-temperature service. There are certain parts of the paper that might be

considered further.

Grain-size data on castings are of little value unless the pouring temperatures are of the same order as those used in the foundry, namely, about 1400 F. Furthermore, the size of the section has a very important effect; grain-size data are relatively meaningless unless data on fairly heavy sections are included.

Specifically, what pouring temperatures and section size were used to obtain the data in Table 5? If these are not representative of foundry conditions, do the authors have data on the grain size of flat sections ½ in. thick poured at 1400 to 1450 F?

The tensile properties listed in Table 6 are apt to be misleading unless the time of exposure of the casting in service at 600 F is relatively short, namely, a matter of a few minutes. If long-time service exposures are contemplated, the alloys should be stabilized at the temperature of test for a prolonged period at the testing temperature or for a shorter period at a temperature slightly higher than the testing temperature. For example, the following are the tensile properties of EM62-HTA alloy (6.0 per cent rare earth, 1.65 per cent Mn) at 600 F before and after stabilizing 24 hr at 650 F prior to test at 600 F:

| | Tensile Properties at 600 F | | | | | |
|-----------|-----------------------------|-----------------------------|----------------------------|--|--|--|
| Condition | % Elongation | · Yield Strength, psi | Tensile Strength psi | | | |
| ЕМ62-НТА | 45.0 | 8,000 | 12,800 | | | |
| EM62-HTAS | 60.0 | 6,700 | 10,500 | | | |

The data on the EM62 alloy were obtained at Battelle after it had been heat treated and aged and then tested with and without stabilization for 24 hr at 650 F. In all probability, the data on EK30 and EZ33 (ZRE1) alloys at 600 F, as listed in Table 6, would be more adversely affected by stabilization prior to testing. The effects of stabilization are also appreciable at 500 F, but become less important as the testing temperature is decreased.

The authors rather loosely lump all the cerium-containing alloys together as having similar creep properties. For example, the authors discount the effect of manganese in the 6 per cent cerium alloys. Actually, data obtained at Battelle clearly show that the creep rate of E6 (6 per cent rare earth, 0 per cent Mn) is about ten times greater than EM62 alloy (6 per cent earth, 1.6 per cent Mn) under a 2500 psi load at 600 F. Furthermore, it is quite possible to formulate magnesium-cerium alloys having far better load-carrying capacity at 600 F than either EK30 or the ZRE1 type of alloy. For example, the creep limits of these

TABLE C

| | | As-Cast and Aged | | | Heat Treated and Aged | | | | |
|-------------------|---------------|------------------|------|-------|--|------|------|-------|---------------------------------------|
| Alloy Composi- | Testing Temp. | | 1000 |) psi | (1000 psi) Creep Str. 0.2% Total | | 1000 |) psi | Creep Str. 0.2% Total Extension |
| tion °F | | %E | TYS | TS | Extension | %E | TYS | TS | (1000 psi) |
| EK30 | 70 | 0.5 | 15.5 | 19.9 | - | 2.4 | 16.5 | 22.9 | - |
| | 400 | 15.7 | 12.4 | 18.7 | 7.7 | 13.1 | 14:0 | 20.0 | 7.2 |
| | 600 | 56.2 | 6.6 | 10.1 | 1.5 | 69.6 | 7.7 | 11.8 | 1.5 |
| EK31 | 70 | 1.0 | 17.3 | 20.8 | | 2.9 | 19.8 | 26.5 | - |
| | 400 | 14.5 | 12.9 | 19.7 | 7.7 | 13.0 | 16.5 | 21.4 | 8.0 |
| | 600 | 54.5 | 7.7 | 11.7 | 1.3 | 52.0 | 9.2 | 13.3 | 1.5 |

² Supervisor, Battelle Memorial Institute, Columbus, Ohio.

alloys listed in Table 8 are about 1700 to 1900 lb, which loading will produce a 0.5 per cent total deformation in 100 hr at 600 F. Magnesium-cerium alloys have been developed at Battelle which, under these test conditions, will have a creep limit of about 3200 psi or nearly twice that listed for ZK30 or EZ33 (ZRE1) alloys listed in Table 8.

We feel confident that, from the standpoint of load-carrying capacity at 600 F, the magnesium-cerium alloys have been developed which will considerably out-perform either the EK30 or the ZRE1 type of alloy, in respect to load-carrying capacity at 600 F. Furthermore, as the authors have indicated, the zirconium alloys present a considerable problem in the foundry to keep the zirconium in the alloy.

K. E. Nelson (Authors' Reply to Dr. Eastwood): Dr. Eastwood's comments are very interesting and I am happy to answer them.

The grain sizes reported in Table 5 were taken from the reduced section of ½-in. diameter standard tensile bars. These bars were poured at 1400 F. The panels from which test bars were machined, tested and described in Table 9 were ½ in. thick and were poured at 1425 F. The grain sizes in Fig. 1 as well as in Table 8 were obtained from various casting section sizes. Each set of analyses in Table 8 represents a given casting (not necessarily the same type of casting on each analysis). As expected, within each casting, the finer grains were obtained in thin sections and the coarser grains found in the heavier sections. The data reported in Fig. 1 show the difference in grain size with Zr content as well as section size.

A footnote in Table 6 was inadvertently omitted in the preprint of this paper. The elevated temperature tensile bars compared in this table were held 10 min at temperature prior to testing. We are in agreement with Dr. Eastwood that prolonged exposure at elevated temperatures results in lower tensile properties than short time exposures.

The present interest in magnesium alloys at elevated temperatures centers around applications at approximately 400 F. It is true that EK30 or EZ33 type of alloys do not have the load-carrying capacity at 600 F that a few other Mg-rare earth combinations have. Table 7 lists the creep limit of E3 alloy at 0.5 per cent total extension as 2800 psi. This same parameter of creep in EM62 has been determined by us as 3200 psi. As indicated in the paper these alloys are not as easy to handle as EK30 alloy in production foundry operations. They are, therefore, less desirable than EK30 when both properties and castability are considered.

The following creep data further confirm our opinion that EK30 alloy compares satisfactorily with other alloy compositions at 400 F.

| Composition | Testing Temp. | Creep Limit 0.5% Total Expansion |
|-------------|---------------|-------------------------------------|
| | (° F) | psi |
| EK30-HTA | 400 | 10,100 |
| | 600 | 1,700 |
| E3-HTA | 400 | 10,200 |
| | 600 | 2,800 |
| E6-HTA | 400 | 11,800 |
| | 600 | 3,000 |
| EM62-HTA | 400 | 11.000 |
| | 600 | 3,200 |

A STUDY OF THE EFFECT OF VARIOUS BINDERS AND ADDITIVES ON HOT STRENGTH OF MOLDING SANDS*

By

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ABSTRACT

Simplified synthetically-bonded sand mixtures containing western bentonite as the primary binder were tested for hot compressive strength at temperatures from 200 to 2500 F. A reference sand mixture containing 2 per cent western bentonite and 3 per cent water was selected for comparison with mixtures containing other clays and to evaluate the effects of secondary sand binders and other additions.

It was found that the clay used establishes the general pattern of the hot strength temperature curve. Clay binders produce relatively high hot strengths in comparison to other binders, especially in the pre-fusion range. Hot strength was found to increase with increase in both clay and water contents in the range studied.

Organic binders were studied both as a single binder and as a secondary binder in combination with western bentonite. It was found that these secondary sand binders modify the strength of the clay binder, generally by increasing strength in the 400 to 800 F range. At higher temperatures, the effect produced depends on the nature of the material, some causing increases and some decreases in the strength developed by the clay binder.

Certain materials, called additives rather than binders because they do not contribute to green strength, were also studied. It was found that these materials have a strong effect on hot strength over a wide range of temperatures. Additions of silicon flour and iron oxide increase hot strength, while additions of

wood flour decreases hot strength.

MOLDING SAND MUST POSSESS an unusual combination of properties to successfully fulfill the requirements of the various steps in the molding and casting cycle. Initially the sand must be relatively weak and "flowable" so as to conform readily to the pattern during the molding operation. After molding the sand should have sufficient green strength to withstand handling. During pouring, surface hot strength must be developed rapidly to resist severe thermal shock and erosion by heavy turbulent liquids; somewhat later general strength must be developed by the entire mold to support the fluid pressure of the liquid metal and remain true to pattern dimensions. After the metal has solidified the sand must revert to a friable

material which will allow easy removal of the castings. The used sand should then be amenable to remixing and use for another cycle.

Most molding sands actually develop the general sequence of characteristics described in a qualitative sense. However, successful casting requires that the sand have specific quantitative values of strength and ductility for specific types of castings. Inasmuch as little basic information is available relative to the effect of various sand components on sand characteristics it is necessary to develop specific mixtures by trial and error. This report presents such basic information as data on the effect of various binders and additives on hot strength of synthetic molding sands in the range of 200 to 2500 F. No attempt has been made to establish the strengths required for eliminating various defects or producing successfully various types of castings.

Scope of Present Report

The simplified synthetically-bonded sand mixtures used in this study of hot compressive strength consist of graded sand, selected sand binders, additives and water. Clay may be considered as the primary binder for mixtures of this type. Additional materials, fall in the general categories of organic binders and additives, which may be introduced to control texture, strength, and other characteristics. The organic binders may be considered secondary sand binders or modifiers of the primary clay binder. Additives serve as fillers or in some other non-binding capacity and are not considered to add coherence.

The various commercial clay binders vary considerably in the amount and nature of the principal clay minerals present. For example, the principal mineral in western bentonites is sodium montmorillonite; in southern bentonites it is calcium montmorillonite; and in fire clay it varies widely, but is principally kaolin, with smaller amounts of other minerals such as montmorillenite or illite. The strengths of the various clays cover a wide range. In this investigation the amount of clay binder for study was selected so as to have practical foundry workability, a green strength adequate for handling, and a hot strength within the limits of the testing equipment.

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It is well known that the strength of sand-clay-water mixtures can be varied by changing the clay or water contents. In order to evaluate these effects quantitatively, western bentonite was tested in various concentrations and with various water contents. A reference mixture of western bentonite was also used to evaluate the effects of a number of modifiers and additives. Foundry experience has shown that the commonly used clay binders are affected similarly by like modifications. Thus, while any one of the various clays could have been selected for detailed study, western bentonite was chosen because it represents the best known and most commonly used foundry clay. Water content in all mixtures was held as close as possible to 3 per cent except in those mixtures of western bentonite concerned with the study of water as a variable.

The modifiers were tested as single binders in a 1 per cent mixture with sand and water as well as in a mixture containing sand, 2 per cent western bentonite, 1 per cent of the organic binder, and 3 per cent water. The effect of the modifier alone as well as its effect

with clay binders was shown.

In the case of the various additives studied, 1 per cent was not enough to produce a definite effect on the sand mixture. Iron oxide and wood flour were therefore tested in a 5 per cent concentration and silica flour in both 5 and 20 per cent concentrations. Additives of this type are used in synthetically bonded sands only when required by unusual molding conditions.

Previous Work on the Problem

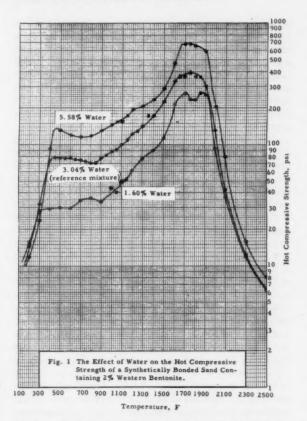
A literature survey was made of investigations on refractory materials, ceramics and molding sands at elevated temperatures; a selected bibliography is appended in chronological order. Expansion and contraction on slow heating and on shock heating by sudden immersion in a hot furnace have been studied by various investigators.^{2,4,15} Bodin¹ studied compressive strength of refractories using small cube specimens. Dietert and his co-workers, 4,8,11,33,36,44,47 developed a furnace heated by silicon carbide elements and investigated ranges of strength at various temperatures which correlate with the production of good castings for various metals and alloys. A project concerned with improving elevated temperature testing procedures has been carried on at Cornell University by the Sand Division of the American Foundrymen's Society and reports of this work have been presented by York,10 Ehrhart,15 Young,19.26 Williams,34,40,42 and Ries.³² Expansion and compressive strengths of 4 per cent western bentonite and 10 per cent fire clay mixtures have been studied extensively. Grim and his coworkers^{5,12,13,39,45} have made important fundamental contributions to the knowledge of clay minerals and the mechanism by which clays develop green and dry strength in molding sands. Kleber and Myer⁴¹ studied practical applications of high temperature tests in the steel foundry. Salz48 studied high temperature tests in relation to cast iron founding. In spite of the large amount of material written about refractories and molding sands, there is very little information on the effect of specific, commonly used materials on the hot strength properties of sand-clay aggregates.

Discussion of Equipment and Methods

Standard methods for sand testing as specified by the American Foundrymen's Society were used wherever applicable methods were available. The sand mixtures were made up as carefully weighed 3000gram (dry weight) batches. The sand used was A.F.S. Secondary Standard Test Sand. Percentages of sand and binder or other additives were based on the total weight before adding water. All batches were placed in an 18-in. laboratory muller and dry mixed 2 min. Water was added in the mixer after which mixing was continued for 5 min. Moisture content was determined after mixing by the loss in weight on drying at 221 to 230 F and expressed as a percentage of the moist sample weight. The mixed sand then was passed through a 4-mesh foundry screen into half-gallon mason jars which were immediately sealed with rubber stoppers, care being taken to avoid compacting the sand in the jar. The jars were inverted occasionally to prevent concentration of moisture. Test specimens were prepared by weighing out sufficient sand to make a cylindrical specimen 11/8 in. in diameter by 2 in. long. The sand was compacted by three blows of a 11/8-in. diameter Dietert sand rammer in a specimen tube with a high quality ground and polished inside surface. All specimens were maintained within a tolerance of \pm .02 in.

Two different furnaces were used for making the compressive strength tests—one was heated by a carborundum "glotube" and the other by a group of six "globars." The furnaces were compared by testing several duplicate specimens in both furnaces at several temperatures, and the results were in very close agreement. The globar furnace was much preferred, however, because it has an observation port so that specimens may be seen and examined for cracks or other defects during heating, and also because it has an improved loading and weighing system. Weighing in the glotube furnace is accomplished by a Bourdon tube gage with a rider, and in the globar furnace by a mercury manometer and check valve.

In most cases two to four hot compressive strength tests were made at each temperature. A total of 101 tests were made at 34 temperatures. At ten of the temperatures four tests were made, at twelve temperatures three tests were made, at twelve temperatures two tests were made and at one temperature only one test was made. The standard deviations from the average at each temperature are shown both in psi and as a percentage of the average test value at that temperature. The average standard deviation in per cent for all of the temperatures is 3.49 per cent. This statistic implies that the chance of a single test falling in the range of the average $\pm 31/2$ per cent is better than 2 to 1. After the original tests were completed the graph was examined critically and eleven points were suspected of possible inaccuracy. These points were checked with a total of 47 additional tests. Most of the checks points were so close to the original points that in nine of the eleven cases the original averages were used. In the other two cases the check test values were used because they were more nearly in agreement with the values for nearby temperatures and it was



assumed that some of the specimens in the first group of tests were defective.

The outlined procedures were followed throughout this investigation. Smooth curves were drawn through the plotted points wherever possible. In a few cases, however, the points were somewhat erratic over limited ranges and the curves were therefore shown as dotted lines indicating some uncertainty in this range. From the fact that the average deviation was found to be 3.49 per cent of the test value, deviations of a point from a smooth curve considerably greater than this amount should be given consideration of validity depending on the general form of curve at the point.

The term "dry strength" and "maximum hot strength" have been adopted to simplify the discussion of data. "Dry strength" refers to the highest strength level developed in the range of 200 to 500 F by heating at those temperatures for 12 min. It should be noted that this definition does not correspond to that given in the FOUNDRY SAND TESTING HANDBOOK as strength produced by drying at 221 to 230 F for 2 hr for practical purposes the two values are equal. "Maximum hot strength" refers to the highest strength developed in the range of 1500 to 2200 F which precedes extensive fusion.

Properties of Simplified Western Bentonite Mixtures

General Strength-Temperature Relationship— In order to establish a standard sand mixture with which other mixtures could be compared, a simple synthetically bonded sand was prepared containing 98 per cent A.F.S. Secondary Standard Test Sand and 2 per cent western bentonite. Water was added to make

 3.0 ± 0.1 per cent based on the moist weight. The hot compressive strength of this mixture is shown as the middle curve in Fig. 1. A rapid increase in strength occurs between 200 and 400 F. It is in this range that free moisture is evaporated. It has been found that if the specimen is exposed for a long time at a temperature of 212 F it attains the same strength. In these tests the short exposure time of only 12 min required a considerably higher temperature for complete evaporation of free moisture. As the temperature is increased from 400 to 850 F the strength drops slightly. From 850 to 1750 F the strength again increases steadily and sharply.

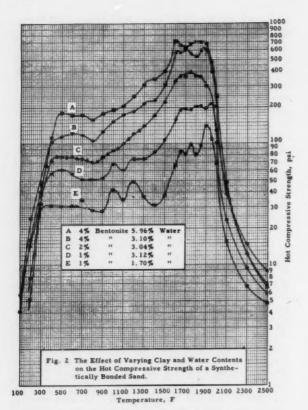
As temperature is raised above 1750 F, the strength drops slowly from 1750 to 1900 F and then rapidly from 1900 to 2500 F where the tests were discontinued. At 2500 F the hot strength was reduced to 6.4 psi.

The mechanism by which clay binders develop gradually increasing strength in the range of 200 to 1800 F has not been ascertained. As a working hypothesis extension may be made of the classic investigations by Grim and Cuthbert39,45 of the process by which "dry strength" is developed. The essential feature of this process depends upon the gradual elimination of water arranged as multilayers of oriented dipoles between crystal plates of montmorillonite. The interaction of the flakes and water are assumed to develop strong bonding forces which resist volatilization of the water at temperatures considerably in excess of the normal boiling point. At temperatures greater than 800 F the attractive forces are considered to be gradually overcome and successive layers of the water molecules are disrupted thus drawing the crystal plates closer together. The bonding forces between flakes thereby become greater, resulting in a progressive increase of hot strength. The occurrence of physical-chemical reactions in this range should also be considered. No information, however, is available in this connection.

A gradual loss of weight over this temperature range has been noted for both southern and western bentonite sand mixtures. This may be interpreted as evidence for gradual volatilization of water in this range as postulated by the mechanism described.

Effect of Water Content—In order to study the effect of water, the standard reference mixture of 2 per cent western bentonite and secondary standard test sand was mixed with a low water content (1.60 per cent) and a high water content (5.58 per cent). In Fig. 1 these curves are compared with the standard mixture with 3.04 per cent water. The shape of the curves is approximately the same regardless of water content except that the low-water sample seems to be slightly more erratic. Doubling the water content nearly doubles the hot strength in the range from 400 to 1900 F. From 1900 to 2500 F the effect of water is still apparent but not pronounced. This may be due to improved mobility of the clay crystals in the green state in the high water mixtures resulting in more efficient positioning of the clay crystals. It is apparent that water content has a very strong effect on hot strength and that it must be carefully controlled.

At the low temperature end, the curves cross. This is because more water must be evaporated from the



high-moisture specimens and they are therefore slower in developing dry strength.

Effect of Clay Content-Figure 2 shows the effect of varying the clay content on the form of the strengthtemperature curve. The central curve is that of the reference mixture containing 2 per cent western bentonite and 3.04 per cent water. Mixtures were prepared with half and twice this amount of bentonite, keeping the water content constant. These mixtures, contained 1 per cent bentonite with 3.12 per cent moisture, and 4 per cent bentonite with 3.10 per cent moisture. In order to study the effect of high and low clay contents with proportional amounts of water two further mixtures were made, and the test results are shown as the top and bottom curves on Fig. 3. These mixtures contained 1 per cent clay, with 1.70 per cent water, and 4 per cent clay with 5.96 per cent water. The general shape of all the curves is the same in that a level of dry strength is achieved at some temperature from 300 to 500 F and as temperature is raised, the strength does not change appreciably for several hundred degrees. Thereafter, at about 900 F an increase in strength begins which continues until the temperature of maximum strength is reached. For western bentonites the zone of maximum hot strength is relatively broad and extends from 1600 to 1900 F. At temperatures above 1900 F the strength drops sharply to less than 10 psi at 2500 F.

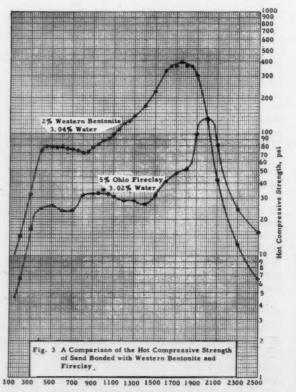
From the curves for 1, 2 and 4 per cent bentonite at approximately 3 per cent water content it may be seen that doubling the bentonite increases the strength about 50 per cent in the temperature range from 400 to 1900 F. If, however, the water is increased propor-

tionately with bentonite (1 per cent bentonite with 1.7 per cent water, 2 per cent bentonite with 3.04 per cent water and 4 per cent bentonite with 5.96 per cent water), the effect of increasing the bentonite is much larger; doubling the bentonite content then approximately doubles the strength in the 400 to 1900 F zone. It should be recognized that these relationships hold only for the limited range of water and clay contents specified.

The strength curve for 1 per cent bentonite with 1.70 per cent water seems to be somewhat erratic; several small maxima may be seen. It was thought that occurrence of the peaks might be due to experimental error but the variation amounts to 10 to 40 per cent of the test value and the indication of similar maxima in the 1 per cent bentonite, 3.12 per cent H₂O curve suggests that the maxima may actually occur. Indications of such multiple peaks in the other curves may also be seen in the range from 1600 to 1900 F.

Comparison of Various Other Clay Mixtures With Western Bentonite Mixtures

Since clays other than western bentonite are sometimes used as the primary binder in foundry sand, several other clays were studied for comparison with western bentonite. These included a commercial fire clay from Akron, Ohio (containing kaolin as its principal mineral), which is used by steel foundries particularly for heavy castings. White kaolin shows the effect of the mineral in as pure a form as it is readily available. A southern bentonite is used extensively for cast iron sands, and an Albany sand containing the clay with which it occurs in nature.

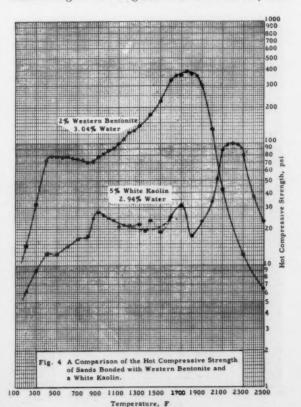


Temperature, F

Ohio Fire Clay-Figure 3 shows the strength-temperature relation of the Ohio fire clay in a 5 per cent mixture with sand as well as the western bentonite reference mixture. Water content was 3.02 per cent. Although two and a half times as much fire clay as western bentonite was used in order to make a workable mixture, the hot strength at most temperatures was well below that of bentonite. Drying produces a strength of 25 psi at 400 F. Little change in strength occurs from 400 to 1400 F. From 1400 to 1800 F a gradual increase in strength takes place. From 1800 to 2000 F the strength increases rapidly to a maximum of 136 psi. From 2000 to 2500 F the fire clay exceeds the strength of the bentonite reference mixture. The principal difference in these clays, aside from the general lower strength level of the fire clay mixture, is in the temperature at which the maximum hot strength occurs. The fire clay maximum occurs at about 250 F higher than that of bentonite.

White Kaolin-Figure 4 shows the hot strength range for a medicinal white kaolin, in a 5 per cent mixture with a water content of 2.94 per cent. This mixture reaches a dry strength of 13 psi at 400 F. The strength is somewhat erratic, without the usual plateau from 400 to 900 F then increases rapidly from 1800 to 2100 F reaching a maximum of 100 psi, after which it again decreases in the range from 2200 to

This clay develops its maximum hot strength at a higher temperature than any of the other clays which were studied, having its maximum 200 F higher than the fire clay and 450 F higher than the western bentonite. Its general strength, however, is relatively low.



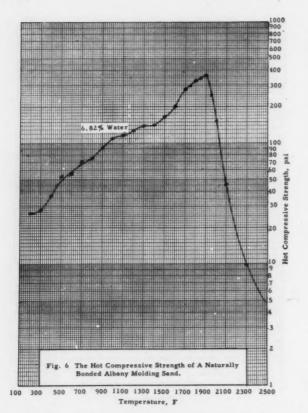
300 200 A Comparison of the Hot Compressive Strength Bentonites.

700 900 1100 1300 1500 1700 1900 2100 2300 250

Southern Bentonite-A commercial southern bentonite, principally calcium montmorillonite was studied in a 2 per cent mixture at 3.12 per cent water content. The results are shown in Fig. 5. It has roughly one-fourth to one-half of the hot strength of western bentonite. A dry strength of 40 psi is reached at 450 F which decreases to 30 psi at 1450 F. Strength increases rapidly from 1450 F to a maximum of 90 psi at 1600 F and then decreases to 3.4 psi at 2500 F.

The strength-temperature relation of southern bentonite has the same general features as western bentonite. It is characterized, however, by a relatively low hot strength and a maximum hot strength at 1600 F which is 150 F below that of western bentonite. This curve disagrees considerably with the work published by Dunbeck¹⁷ which indicated a maximum of 35 psi at 1000 F for a mixture containing 4 per cent southern bentonite.

Albany Sand (Natural Clay Bond)-Figure 6 shows the strength-temperature of a fine-grained Albany molding sand. This sand is not directly comparable to synthetically bonded sands because its grain size and distribution are not the same. Albany sand has a median grain size of approximately 100 microns, with 3 per cent smaller than 1 micron. Silt is abundant, some organic matter is present, and sorting is very poor. It was found that 6.87 per cent water was necessary to produce a workable mixture. The dry strength plateau developed by the synthetic sands is not obtained; instead from 100 F to 1900 F there is a steady increase to the maximum hot strength of 360 psi, after which the strength drops rapidly to 10 psi at 2300 F. It is generally considered that Albany is a



low fusion sand, hence the location of the hot strength maximum as high as 1900 F was surprising. Severe shrinkage takes place from 1900 to 2300 F and the sand fires to a hard body which does not disintegrate on cooling. Gas evolution from 2300 to 2500 F caused gross swelling of the sample which is called bloating.

Effect of Various Organic Modifiers on a Reference Mixture of Western Bentonite

Gelatinized Corn Flours—Gelatinized corn flours, next to clay, are the most commonly used binders in the steel foundry. They are usually used as a secondary binder with clay as the primary binder. Two mixtures were studied one having 1 per cent corn flour and 99 per cent sand at a moisture content of 3.04 per cent and the other a combination with 1 per cent corn flour, 2 per cent western bentonite and 97 per cent sand at 3.04 per cent water content. In Fig. 7, the hot strengths of these mixtures are compared with the standard 2 per cent western bentonite mixture.

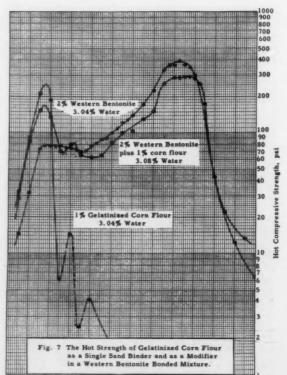
With corn flour alone as the binder, the strength increases rapidly to a maximum of 250 psi at 450 F. As the temperature is increased, the strength drops to 6.1 psi at 600 F. When 700 F is reached the strength increases to 14.3 psi. This behavior was repeated again with a minimum of 2.45 psi at 800 F and a maximum of 4.2 psi at 900 F, after which the strength became too low to be measured accurately. The minima and maxima shown are possibly associated with the combustion characteristics of this organic binder. At 1200 to 1300 F the specimen becomes light in color indicating that the carbonaceous materials are completely removed. At this point the strength is less than 1 psi.

When the corn flour binder is combined with bentonite, the curve characteristics are similar to those of the western bentonite reference curve but with modifications occasioned by the presence of the corn flour. At 450 F a maximum of 170 psi is reached, which drops to a minimum of 70 psi at 600 F. This corresponds with the first minimum for the series with corn flour as the only binder. At about 730 F a maximum of 82 psi is reached corresponding with the second maximum in the corn flour curve. A slight drop in strength occurs at approximately 800 F and from 800 to 1900 F the strength of the combined binders is less than that of bentonite alone.

From these curves it appears that gelatinized corn flour serves two functions; first, it provides "early hot strength" which may be developed by air or oven drying or by the heat of the poured metal, and second, it burns out just before the sand reaches the quartz inversion point, thus providing a small amount of space into which the sand grains can expand. This reduces the possibility of cracking the mold surfaces, as is noted in practice.

Dextrine—Foundry dextrine, also a corn product, which is over 90 per cent soluble in water, was studied in the same percentages as those used for the gelatinized corn flour; 1 per cent as a single binder with 3.02 per cent water, and 1 per cent in conjunction with 2 per cent western bentonite and 3.00 per cent water. The hot strength curves are compared with the 2 per cent western bentonite reference mixture in Fig. 8.

Dextrine produces very high early dry strength (240 psi at a 350 F). It also has a temperature range (450 to 580 F) where strength drops very suddenly and then



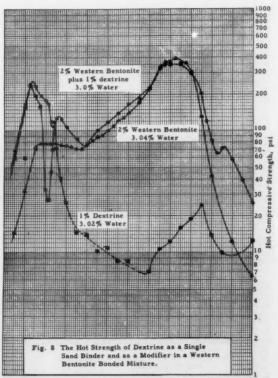
100 300 500 700 900 1100 1300 1500 1700 1900 2100 2300 2500 Temperature, F

recovers markedly. After reaching a second maximum of 118 psi at 580 F the strength drops quickly to 14.5 psi at 800 F. At higher temperature dextrine behaves differently from corn flour in that it maintains an appreciable strength at high temperatures. There is a minimum of 7 psi at 1500 F, a maximum of 24.5 psi at 2000 and at 2500 F, the highest temperature tested, the strength is 12.5 psi.

When dextrine is combined with western bentonite it increases hot strength over most of the temperature range. The increase is especially evident at 375 F, 625 F and 2200 F. These maxima occur at slightly higher temperatures than those for the 1 per cent dextrine mixture without bentonite. From 2200 to 2500 F dextrine has a strong effect on the sand, producing a hot strength three to four times that produced by bentonite alone.

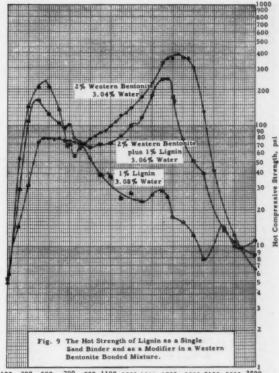
Dextrine, as shown by its effect on the reference western bentonite mixture, is very useful for producing early hot strength which may help resist cutting and erosion. At high temperatures it adds slightly to the strength of the clay rather than decreasing it as do the gelatinized corn flours.

Lignin—Lignin is a by-product in the sulphite process of paper manufacture. Some of this material is used both in liquid and powder form as a molding and core sand binder. A typical powdered lignin binder was selected and tested in the same percentages as corn flour and dextrine with the results shown in Fig. 9. Like corn flour and dextrine it develops high dry strength. It begins to break down at 500 F and the strength from 500 to 2000 F drops steadily to about 8 psi. From 2000 F to 2500 F there is a slight in-



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Temperature, F



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crease in strength. Like dextrine, lignin produces strength effects at temperatures far above the point where it is expected to burn out completely.

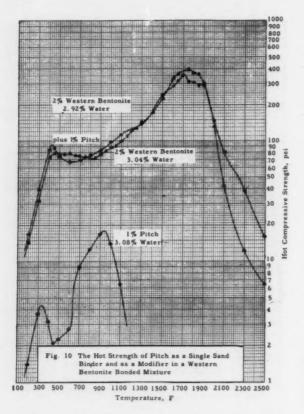
When lignin is combined with bentonite (1 and 2 per cent respectively) it increases hot strength considerably at temperatures up to 800 F. Above this point the lignin causes the mixture to be weaker than it would be with bentonite alone. The high temperature maximum at 1620 F is narrow and sharp, lower than that of bentonite alone and shifted toward lower temperatures. At 1850 F 2 per cent bentonite produces 360 psi while 2 per cent bentonite plus 1 per cent lignin produces only 60 psi.

From this it appears that lignin may be useful in reducing hot strength above 800 F, if this is necessary, and at the same time provide dry strength and possibly some quick hot strength to resist erosion by flowing metal.

Lignin binders are hygroscopic hence care should be exercised when they are used to see that the molds are used soon after being made or are adequately dried in order to avoid excessive water content.

Pitch—Pitch is not widely used in molding sands but finds considerable use in cores, particularly wherever a core is made with a sand containing clay where an oil core binder cannot be used economically. A finely ground commercial grade as supplied for foundry use was selected and mixtures were made containing 1 per cent pitch and a combination of 1 per cent pitch with 2 per cent western bentonite. Water contents were 3.08 and 2.92 per cent, respectively. The hot strength curves are shown in Fig. 10.

The hot strength of the 1 per cent pitch mixture is



unlike any binder previously tested. In general its strength is very low. A maximum of about 4 psi occurs at 350 F due to evaporation of the moisture and another, 18 psi at 950 F. From 1000 to 1100 F the strength drops rapidly and above this point no measurable strengths were found.

The hot strength of sand containing 2 per cent western bentonite is only slightly changed by the addition of pitch. The two maxima developed by pitch alone are reflected in the curve for the combined binders by slightly higher strengths over a limited range of temperatures. Pitch has its most pronounced effect above 2000 F where it adds considerably to hot strength. At 2300 F, for instance, the strength is increased from 12.3 to 38.5 psi by the addition of 1 per cent of pitch.

The failure of pitch to produce any pronounced change in the hot strength of a sand-clay-water mixture except at high temperatures does not condemn it as a binder. It serves a useful purpose in high strength cores containing clay which are first baked and then cooled to room temperature. Under these conditions the pitch melts during baking and solidifies during cooling, giving the core dry or baked strength so that it may be handled and placed in the mold.

Effect of Various Additives on a Western Bentonite Reference Mixture

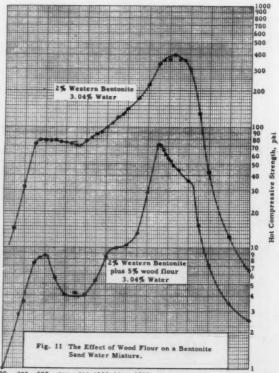
Wood Flour—Wood flour, which has previously been reported⁴³ as having a beneficial effect in preventing veining defects in phosphor bronze castings, was tested at 5 per cent content with 2 per cent western bentonite and 3.06 per cent water. Wood flour as

a single additive was not tested. Figure 11 shows the strength-temperature curve for the wood flour-bentonite combination with that of the 2 per cent bentonite reference mixture.

Examination of the curve shows that wood flour has the most drastic effect of any material studied in reducing hot strength. At 500 F the dry strength maximum is reduced from 78 psi for bentonite to 8.5 psi for the bentonite-wood flour combination. Strength then drops to 4 psi from 700 to 900 F. A maximum of 75 psi is reached at 1610 F. From 1610 to 1900 F, although the curve is nearly straight, the type of failure changes completely. From 1610 to 1725 F the specimens break suddenly and with a shear failure along a diagonal plane about 45 to the axis. As 1725 F slight bulging of the sides is noticed with a shear failure and above this temperature the failure is plastic, the cylinder deforming into a barrel shape. All other mixtures investigated break with a shear failure up to 2200 F. With increased temperatures the strength drops rapidly to 2.5 psi at 2500 F. Wood flour appears to be the best agent tested thus far, for reducing the hot strength produced by bentonite at all temperatures studied.

Iron Oxide—A commercial grade of ground iron oxide prepared especially for foundry use was tested in a sand mixture at 5 per cent with 3.02 per cent water and in another 5 per cent mixture with 2 per cent western bentonite and 2.94 per cent water. In Fig. 12 the strengths of these two mixtures are compared with the standard 2 per cent western bentonite mixture.

Iron oxide alone produces little or no strength at



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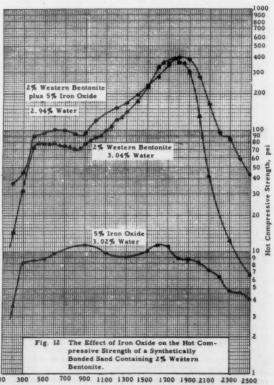
PBI

room temperature and is not usually considered a sand binder. It is sold and used principally as a remedy for veining defects in steel castings but foundry authorities do not agree as to its accomplishment of this result. At temperatures of 300 F and over, 5 per cent iron oxide produces strengths averaging about 10 psi. Above 1700 F the strength decreases gradually to 4 psi at 2500 F.

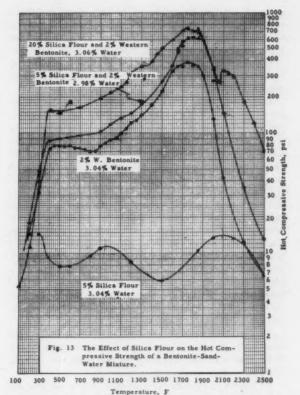
When combined with western bentonite, iron oxide raises the strength of the sand at nearly all temperatures and raises the temperature at which maximum strength occurs by nearly 100 F. At temperatures above 2000 F the iron oxide causes a large increase in strength, which from 2300 to 2500 F amounts to 700 per cent. The curve indicates that iron oxide is useful for increasing hot strength at all temperatures. It also raises the temperature at which the hot strength maximum occurs.

Silica Flour-Silica flour is a common addition to molding sand, particularly for steel castings where it is used as a filler to decrease the amount of void spaces in the sand and thus reduce the penetration of metal into the mold. Silica flour is used in a wide range of concentrations; mixtures occasionally contain as much as 50 per cent.

In studying the effect of silica flour a mixture was made with 5 per cent silica flour and no bentonite and another with 5 per cent silica flour and 2 per cent western bentonite. Water contents were 3.04 and 2.98 per cent respectively. A third mixture contained 2 per cent western bentonite with 20 per cent silica flour and a water content of 3.06 per cent. Figure 13 shows the hot compressive strength of these mixtures along



Temperature, F



with that of the 2 per cent western bentonite mixture

for comparison. The strength of the 5 per cent silica flour mixture varies from 6 to 14 psi with a maximum of 2100 F. The production of hot strength by an inert material cannot be explained except as the result of mechanical action.

When 5 per cent silica flour is combined with 2 per cent western bentonite, the strength is raised over the entire temperature range studied, the largest increases being in the zone about 1700 F. When the silica flour is increased to 20 per cent with 2 per cent bentonite, much higher strengths are produced. The dry strength reaches 150 psi at 400 F and increases to 740 psi at 1750 F. At 2100 F a small rise in strength produces another maximum of 334 psi. At 2500 F the strength drops to 70.5 psi which is, however, very high for this temperature.

Silica flour is an excellent material for increasing the hot compressive strength of sand mixtures over a wide temperature range. It can be added in amounts from very small to very large percentages to produce almost any practical hot strength required by the molding conditions.

Summary

It may be concluded from these data that clay establishes the general shape of the strength-temperature relation of molding sands. By selecting a strong clay, strong sands may be produced with a minimum of clay. Western bentonite produces the greatest hot strength in a given concentration. If lower hot compressive strength is needed without sacrificing green

strength, it may be produced by the use of southern instead of western bentonite. The temperature at which maximum hot strength occurs varies from 1600 F for southern bentonite, to 2200 F for pure kaolin.

Increase in water content of a molding sand increases its hot strength, particularly in the range from

400 to 1900 F.

Increasing clay content also increases hot compressive strength. In order for an increase in clay content to contribute most effectively to strength, the water content should be increased proportionately. Change of clay content is the simplest method of increasing or decreasing hot strength if a change is

needed over the entire temperature range.

Organic binders are useful for modifying hot strength over limited ranges of temperature. These agents generally raise the strength in the zone from 400 to 800 F and thereby provide resistance to erosion and cutting by the flowing metal. Gelatinized corn flour lowers strength slightly from 800 to 2000 F. Dextrine increases strength above 2000 F. Pitch has little effect except at temperatures over 2000 F where it increases strength. Lignin increases hot strength from 400 to 800 F but at higher temperatures reduces strength by large amounts.

Of the various additives studied, wood flour most drastically reduces strength at all temperatures. Wood flour also increases ductility in the range of 1900 to

2100 F.

At 1900 F, a cylindrical specimen, when compressed, deforms into a barrel shape without cracking; without wood flour the specimen breaks at this temperature with very little deformation. The plasticity introduced by wood flour minimizes the cracks which under certain metal conditions produce "vein" defects.

Iron oxide and silica flour increase hot strength at nearly all temperatures. The increase is most pro-

nounced at temperatures over 2000 F.

A summary of the range of strengths which can be produced using 2 per cent western bentonite and 3 per cent moisture content molding sand is presented in Fig. 14 by curves of maximum and minimum strengths produced in this mixture by the addition of various binders and additives. This area may be made still broader by altering water or clay content.

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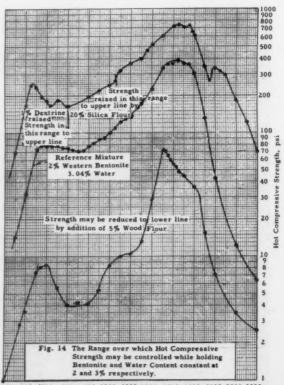
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DISCUSSION

Chairman: J. A. RASSENFOSS, American Steel Foundries, East

Co-Chairman: P. E. KYLE, Cornell University, Ithaca, N. Y.

R. A. WILLEY: 1 Would the author explain what he means by retained strength? Was he referring to strength remaining at 2500 F or was he referring to what is commonly called the retained strength at room temperature?

MR. Morey: I was referring to strength at 2500 F. This is not retained strength, which is strength at room temperature of a specimen previously heated to an elevated temperature. These

were the actual strengths at the test temperature.

CHAIRMAN RASSENFOSS: What was the heating time interval? How long were the specimens heated before you applied the compressive load?

MR. Morey: The specimens were all in the furnace 12 min before the load was applied. That is an arbitrary selection of time but one we found satisfactory.

CHAIRMAN RASSENFOSS: What types of atmospheres were used

MR. MOREY: The atmospheres were oxidizing. There was no controlled atmosphere.

GEO. E. DALBEY: 2 What effect, if any, did mulling time have on these strengths?

MR. Morey: I cannot answer that question. I can merely say that the mulling time used for these specimens was 2 min in the muller dry and then 5 min wet mulling. We found it very difficult to control the sand water contents, so we put a cover over the muller and introduced the water through the cover through a funnel. It was discharged from a tube in front of one of the plows, so the water was turned into the sand and that kept as much as possible off the muller wheels and plows. Wet mulling time in all these tests was 5 min. Total mulling time was 7 min.

MR. RASSENFOSS: In general, with the small amount of work we have done, as the mulling time has increased for clay bonded sand, hot strength at 2500 F goes up.

H. G. LOLLEY: 8 What effect would additional water have on the 5 per cent fireclay mixture. The author tested bentonite against fireclay; but he had the same amount of water

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with 2 per cent bentonite as he did in the 5 per cent fireclay mixture. It is my belief that this was not enough water to

temper the sand with the 5 per cent clay in it.

MR. MOREY: Both the clay and the water content in that fireclay mixture would be low for commercial use. In commercial use we would have to have possibly 10 per cent at least of clay and I expect our water might be up to 6 per cent, but the ratio would still be roughly the same. That is, you would not use much over 6 per cent water in a steel foundry sand.

MR. LOLLEY: In your test though you had the same amount of water in both mixes, did you not?

Mr. Morey: That is correct.

MR. LOLLEY: If we had had enough water in the 5 per cent fireclay mixture to temper it, the same as the bentonite mixture, where does the author think the curve would be? Would

it be comparable with bentonite?

MR. MOREY: The curve would probably be raised. I would expect it to have the same effect as it did in the bentonite mixture (Fig. 1 and 2) where the addition of extra water to the bentonite raised the strength of that mixture. Addition of extra water to a fireclay would raise the strength.

MR. DALBEY: What was the thought behind adopting a ratio

of 1:11/2 bentonite to water?

MR. MOREY: Early tests of the sand made on a dilatometer had a strength limit of 1,000 psi and we had to get our clay content low enough so we would not damage the machine in trying to break the specimens. When specimens get up around 700 psi in the furnace they begin to break the supporting columns and we broke the discs which support the specimen quite rapidly. It was difficult to obtain enough discs to keep up with the work. So for this work we selected a fairly low content of bentonite of 2 per cent, whereas normal foundry mixtures might use 3, 31/2 or 4 per cent. In selecting the fireclay content of 5 per cent, we tried to keep that down proportionately. That is, we kept the fireclay at 21/2 times the content of bentonite in the

belief that the bentonite had about 21/2 times the strength of the fireclay.

In regard to the ratio of 11/2 times the bentonite content for the water, I hesitate to make a statement on that. I would say we tried to keep it within reasonable ranges. We produced a workable mixture at that content. Possibly it is a little too much water for the amount of bentonite we had present, but once we

started on it, we adhered to it all the way through.

E. E. WOODLIFF: 4 This work parallels some work I have done several years ago, although not nearly as complete. The mixtures I made were similar. I loaded them to 10 psi and recorded only the temperature at which the material would no longer carry a 10-psi load. This value giving a straight line in which each material shows its range of strength. In other words, the temperature range in which a material acts is important. That has been plotted for some time. It is gratifying to see what parallel the author gets in this work which is more extensive. However the end points are similar where the author drops the load below 10 psi. This would be the point at which my straight line would end for hot strength.

MR. Morey: In discussing this work we have departed from most of the previous techniques which have been used in measuring hot strength, measuring it every 500 deg. One reason for that is because we were not able to pick up some of the maxima, the exact location of those peaks. Then we began finding some of these interesting effects of minor maxima and minima and we got curious about them. We found many of them as is shown in the graphs, but so far we have not been able to interpret all

of them.

One interesting thing is that we frequently see a maximum and a minimum in one mixture reflected in another mixture in which the first binder was reported in the second.

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OPTIMUM ALUMINUM ADDITIONS IN COMMERCIAL YELLOW BRASS

By R. A. Colton and R. H. Gilbert*

It has long been the custom in many foundries making yellow brass castings to add small amounts of aluminum to the metal prior to pouring. The reasons usually given for making such additions are that the aluminum is necessary to improve the fluidity or castability of the metal, and also to improve the surface finish on the castings made in sand.

While the advocates of adding aluminum to yellow brass in the foundry are numerous, a large number of shops consider this practice as highly detrimental to making quality castings. In fact, some foundries contend that additions of aluminum to yellow brass make it impossible to obtain a satisfactory polished surface on the casting. Other foundrymen claim that aluminum offers no advantages and should be kept out of the metal.

As is the case in many controversial foundry practices, practically no experimental evidence is available for evaluating the effects of aluminum in yellow brass. While the subject has been discussed occasionally in the literature, no controlled experiments have been made to determine the exact effects of aluminum additions in order that the controversy be settled on the basis of experimental data rather than opinion.

One reason why definitive studies may not have been made was the lack of accurate analytical methods for aluminum when present in small amounts. Improved techniques recently developed suggested the possibility of studying this problem with a degree of accuracy.

Castability Improved

Observation of the effects of adding aluminum to commercial yellow brass alloys in a large number of foundries indicated that a definite improvement in castability was obtained when aluminum was added to the metal. By castability or fluidity is meant the ability to fill the mold completely and run the metal satisfactorily into very thin sections. At the same time a number of observations have been made as to the harmful effects which may result from the presence of aluminum, particularly in hardware shops.

It has been observed that the addition of aluminum to the yellow brass or the presence of aluminum in the metal can produce poor surfaces as well as oxide films which may interfere with finishing and polishing.

Since this type of controversy can continue indefinitely unless some experimental evidence is available for evaluating the claims on either side of the question, it appeared to be worth-while to study this matter in an effort to determine more exactly the effects of aluminum when added to yellow brass.

Two separate problems were involved in designing experiments for this work. The first was to determine the effect of aluminum on the tensile properties of commercial yellow brass. This is the simpler of the two problems and was carried out in the conventional manner. The second problem was to design a test to evaluate the general term "castability" with regard to additions of aluminum in yellow brass.

Castability cannot be simply measured and any test used to evaluate it is necessarily a compromise. It is possible, however, to design a practical test which will give useful information and which will simulate actual foundry practice and produce results which can be translated into recommendations that a foundryman can use. Such a test has been devised in this study. It was hoped to determine whether or not aluminum had a specific effect and if so, what amount of aluminum was required to produce this effect.

Materials Used: Two separate heats of commercial yellow brass known as No. 403 were used in this investigation. The analyses of the two heats were as follows:

| Heat R611 | Heat R776 |
|-----------|-----------|
| Comp. % | Comp. % |
| Cu | Cu |
| Sn 1.07 | Sn 1.17 |
| Pb 2.42 | Pb 2.84 |
| Zn 28.60 | Zn |
| Fe 0.27 | Fe 0.37 |
| Ni 0.36 | Ni 0.38 |
| Sb 0.03 | Sb 0.08 |
| S 0.02 | S 0.02 |
| Al 0.008 | Al 0.00 |

These are comparable heats and since each was used in a separate set of experiments, slight differences in chemical composition are not significant.

Melting Practice: In both experiments the general procedure was to melt the ingot down in a forced draft coke-fired crucible furnace, in a No. 60 crucible. The metal was brought to a temperature where it was flaring slightly, the crucible pulled out of the furnace and castings poured. In the process of pouring castings, the aluminum additions were made, as will be explained later.

Mechanical Property Tests: In order to study the effect of aluminum content of the yellow brass on the tensile properties of the alloy, the following procedure was used. After the crucible of molten yellow brass was heated nearly to the flare point and the crucible removed from the furnace, the top surface was skimmed

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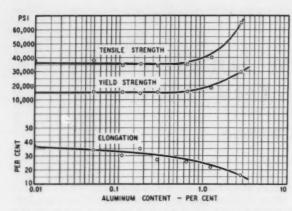


Fig. 1.—Data from Table 1 are plotted to show the effect of aluminum content on the tensile properties of yellow brass. It was necessary to use the logarithmic scale to include the full range of aluminum contents.

TABLE 1—EFFECT OF ALUMINUM ON THE TENSILE — PROPERTIES OF YELLOW BRASS

| No. | Aluminum Content, % | Tensile* Strength, psi | Yield Strength, psi | Elongation, |
|------|------------------------|------------------------|---------------------------|-------------|
| 59-1 | 0.008 | 37600 | 14900 | 37 |
| 59-2 | 0.05 | 38000 | 15500 | 34 |
| 59-3 | 0.11 | 34250 | 15000 | 30 |
| 59-4 | 0.18 | 35500 | 14500 | 35 |
| 59-5 | 0.29 | 34250 | 15250 | 27 |
| 59-6 | 0.65 | 35000 | 15750 | 26 |
| 59-7 | 1.28 | 40000 | 18500 | 22 |
| 59-8 | 2.81 | 65000 | 29750 | 16 |

*The tensile properties are the average of two bars tested for each aluminum content.

and standard ingot molds were poured. Ingot molds were used rather than cast test bars in order to minimize foundry variables, since only the metallurgical effects of aluminum on tensile properties was desired.

The first mold was poured with no aluminum addition, as a head sample. To the remaining metal in the crucible 0.04 per cent pure aluminum wrapped in copper foil was plunged into the bottom of the crucible and allowed to dissolve in the metal. Two stand-

ard ingot molds were then poured. To the remaining metal in the crucible another increment of aluminum was added in the same fashion and ingots poured. This process was followed until all the metal in the crucible had been poured into ingot molds. This procedure gave a series of standard ingots with increasing aluminum content.

Two standard 0.505-in. test bars were cut from the bottom portion of each ingot and broken on the Baldwin-Southwark hydraulic testing machine. Tensile strength, yield strength, and elongation in 2 in. were measured. Drillings for chemical analysis were taken from the top surface of the ingot from a number of locations in order to minimize the possibility of segregation.

Castability Tests: For purposes of estimating the effect of aluminum additions on the castability of yellow brass, a test was devised using as a pattern a letterplate casting. This casting is approximately 1/16 in. thick, 8 in. long, and 2 in. wide. Two such castings were made in a single mold, gated as shown in Fig. 2. The gating arrangement is considered poor from a manufacturing standpoint, but was quite satisfactory for test purposes.

The test consisted of making aluminum additions to the crucible of yellow brass and pouring test castings. Increasing increments of aluminum were added and castings poured until the entire 200 lb in the crucible had been poured out. The first castings poured had no aluminum added and the increments then were 0.005, 0.01, 0.03, 0.05, up to 0.5 per cent aluminum added. From these test castings it was hoped that information on the effect of aluminum on castability could be obtained.

In Table 1 are listed the results of the tensile tests of the bars machined from ingots containing various amounts of aluminum. These data are plotted in Fig. 1. Figures 2 and 3 are photographs of the test castings used to determine the effects of aluminum on castability. Figure 2 shows the casting itself from the cope side with gates and risers included, while Fig. 3 shows only the drag side of the casting. Both views are included since they give a better indication of the effect of aluminum additions than would a single picture.

It can be seen by reference to Table 1 and Fig. 1

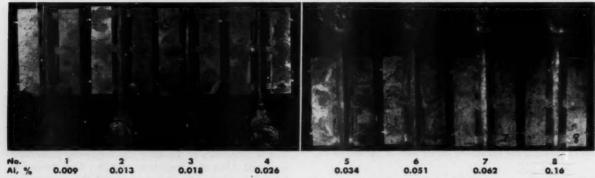


Fig. 2—Cope sides of the test castings show the effect of increasing aluminum content on castability. As the aluminum content increases the oxide films become more pronounced and the castability greater until

(No. 8) the film is fairly uniform and gives the best appearance of those shown. The color of the casting changes from red to red+silver+black to gold+silver to golden at No. 8. No. 1 and 2 had poor castability.

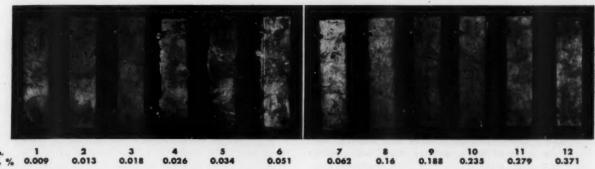


Fig. 3—The effects of aluminum contents on castability are shown by the drag sides of the yellow brass test castings. Castings No. 1-7 show change from no aluminum oxide film through discontinuous thin films,

to an unbroken film at No. 8; castings No. 9-12 show results of increasing amounts of aluminum—the films become visibly thicker, non-uniform and more silvery in color. Aluminum content is shown below each casting.

that, generally speaking, aluminum is a beneficial addition to yellow brass. The tensile and yield strengths are relatively unaffected up to about 0.5 per cent aluminum, and then increase rapidly. The elongation decreases slowly as the aluminum content increases, dropping off about 10 per cent with the first 0.5 per cent of aluminum added.

This means that the ductility normally associated with yellow brasses can be reduced considerably if excessive quantities of aluminum are added to the metal. Certain applications of yellow brasses require ductility. This reduction in elongation found in these tests may be significant for the applications where lower ductility would impair the usefulness of the material.

It is not too surprising that aluminum has a strengthening effect in yellow brass. Since manganese bronze alloys are substantially 60-40 yellow brass with about 1.0 per cent aluminum added, it would be expected that the strength of this metal would also be improved by aluminum additions. It is also to be expected that an increase in strength would be accompanied by a decrease in ductility.

Full Addition Range Covered

It should be pointed out that it was necessary to plot the data on logarithmic paper in order to include the aluminum contents from the very lowest up to the maximum. This type of plotting may give an erroneous impression about the effect of the aluminum.

The change in elongation is a very gradual one and even at 0.5 per cent aluminum addition the ductility has not decreased severely. Consequently, it appears that 0.5 per cent aluminum is a critical addition, considering reduced ductility and increased strength. This should be kept in mind with regard to the effect of aluminum on castability, since it more or less sets an upper limit on the amount of aluminum that can be tolerated in the metal without changing the properties markedly.

In considering the results of the castability test, it would have been desirable to have some method of measuring quantitatively the effect of aluminum on the yellow brass. Several attempts were made, but none of the methods tried gave results which had any great significance. Instead, observations of the castings were made and from the chemical analyses of the cast-

ings an attempt was made to correlate the findings with the aluminum content of each of the castings. The results of these chemical analyses are listed in the captions of Figs. 2 and 3.

From study of the castings, the following observations were made:

1. When the metal was first taken out of the furnace it was flaring greatly. This was before any aluminum addition. This was the hottest metal and yet, when poured into the first mold, a misrun occurred. The metal cooled considerably while successive castings were poured with increasing aluminum additions. It is worth pointing out that the last casting poured with the coldest metal and the highest aluminum addition ran completely.

2. As previously mentioned, the metal was flaring strongly when removed from the furnace and when the first castings were poured. After the initial aluminum addition the flaring was reduced considerably. Apparently the aluminum oxide film that forms over the surface retards flaring and, since smoke in the foundry can be a considerable nuisance, this phenomenon is of interest.

3. The effects of the first aluminum addition are clearly visible on the casting surfaces as shown in Figs. 2 and 3. The original head sample with no aluminum present had a distinct red color and did not fill the mold. With increasing aluminum contents a silvery-gold film appears on the castings. With a difference of only 0.004 per cent aluminum between castings No. 1 and 2, a considerable amount of this aluminum film can be observed on the flat surface of the casting. As the amount of aluminum increases to 0.018 and 0.026 per cent, etc., the amount of this film also increases. It is not, however, until No. 8 where 0.16 per cent aluminum was found that the film becomes completely continuous and covers the casting so that no film edges are visible on the casting surface.

From these observations, it may be said that approximately 0.2 per cent aluminum seems to be the optimum with regard to film formation. With less than 0.2 per cent aluminum the film seems disconnected and does not cover the casting surface completely. With more than 0.2 per cent aluminum, the film seems to thicken, as apparent from the silvery color of the castings, and may be a source of trouble in the casting.

4. Observation of the castings in Fig. 2 shows another interesting point. The first two castings in the series with low aluminum not only did not completely fill the mold and were cold shut, but also show very small quantities of flash off the center runner. As the amount of aluminum increases the amount of flash also increases—it is plainly visible in all castings from No. 4 to No. 8. This is another indication of the strong effect of aluminum on the fluidity and castability of the yellow brass.

5. It was noticed in casting No. 2 that, although the mold was more or less completely filled, the casting itself had cold shuts. This may have been because the thin aluminum oxide films in the metal stream prevented the metal from uniting in the mold and forming a uniform mass. This may well be one of the dangers of having too low aluminum content in the

castings.

An attempt was made to determine the effect of the aluminum additions on the finish of the castings. One 180°; cracks occurred where oxide films are entrapped.

In many cases these films were not detected until the casting had been polished and was then rejected. In this case the difficulty arose when the original charge material had an indeterminate aluminum content; the normal foundry practice was to add 2 oz of aluminum to each 100 lb of melt. When aluminum-free yellow brass ingot was used and the regular aluminum addition made, the castings were satisfactory and no entrapped oxide films were found.

In regard to the question of whether or not aluminum should be added to yellow brass when the castings have to be finished, it can only be said that from these tests it appears that too low aluminum contents or too high aluminum contents may easily lead to trouble. Whether it is possible to satisfactorily control the amount of aluminum is difficult to say.

The evidence given here does indicate, however, that if some knowledge is available as to the amount of aluminum present in the casting and in the metal



Fig. 4—A typical aluminum oxide film is shown on this commercial yellow brass casting. The surface has a golden color with several distinct areas where the oxide film is mottled black and silver. Thin films of this type are frequently trapped in the metal stream, and show up during polishing as fine hairline cracks which are cause for rejecting casting.

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plate from each casting had the surface removed on a belt grinder in an effort to find oxide cracks. This test did not provide any information, however, since the finish was probably too coarse to determine any fine oxide films. It has been observed that it is necessary to polish yellow brass to a high finish to detect these trapped films.

One of the commonest objections made to using aluminum in yellow brass to improve castability is that it is impossible to get a good finish on a casting where the metal has had aluminum added. This is entirely possible and is verified by this work. It is necessary to have a rather carefully controlled aluminum addition to prevent the presence of either very thin oxide films or very heavy oxide films, either of which may easily be trapped in the metal and produce defects in the finished casting.

When the aluminum addition ran about 0.2 per cent the film on the surface appeared to be quite uniform, and the casting itself had a good finish. It is an interesting observation that industrial practice in many shops casting yellow brass has tended toward the use of an aluminum addition of about 0.2 per cent. Since this is an empirical solution based on what can be considered a large amount of experience, it appears that the figure of 0.2 per cent is fairly well founded. The evidence from this test confirms such an addition.

An interesting example of the possible harmful effects of aluminum additions is shown in Figs. 4 and 5, which are photographs of commercially made brass letterplate castings. Figure 4 shows the nature of the casting surface when entrapped oxide films are present. Thin black oxide films are associated with gate areas of the casting. The casting in Fig. 5 has been bent

charged in the furnace, it should be possible to control the aluminum content rather closely. This is especially true if aluminum-free yellow brass ingot is used. Using close control, it does seem possible to improve the castability greatly, reduce the smoke, and improve casting finish by the use of aluminum.

As to the effect of aluminum on tensile properties, it can be seen that the optimum aluminum addition of 0.2 per cent is well within the range where aluminum has any marked effect on the tensile properties. The low addition would have a relatively insignificant hardening effect. It is possible, however, that in addition to hardening the metal with higher aluminum additions considerable trouble might be experienced from trapped oxide films to be found in the casting. The higher the amount of aluminum the more extensive and thicker are the oxide films.

Summary and Conclusions

Experiments have been conducted to determine the effect of aluminum on both the tensile properties and castability of commercial yellow brass ingot. As a result of the data obtained in these experiments and observations made during making the castings involved, the following conclusions can be drawn:

1. Aluminum contents up to 0.5 per cent have practically no effect on the tensile and yield strengths of commercial yellow brass, while elongation is reduced continuously as aluminum content increases. When the aluminum contents are in excess of 0.5 per cent the tensile and yield strengths increase rapidly, accompanied by a more marked decline in the elongation. This is as expected since aluminum is known to strengthen yellow brass alloys, and the lowered ductility will be expected with the increasing strength.

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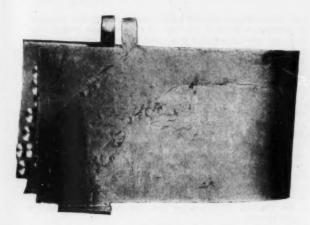


Fig. 5-Bending at 180° revealed the presence of entrapped aluminum oxide films in this casting. Such films frequently show up only after grinding and polishing operations and are cause for rejection. In this case the undesirable effects of aluminum were eliminated when the foundry began using yellow brass ingot with less than 0.01 per cent aluminum contained instead of material of indeterminate aluminum content.

2. It has been observed that the presence of aluminum in casting yellow brass reduces considerably the amount of smoke or flare normally experienced in casting this metal. Also, it has been shown that aluminum increases the castability to the extent that the metal can be poured at a lower temperature than when no aluminum is added. This means that the smoke, or flare, or fume can be reduced to a minimum by using temperatures below the flare point in pouring many castings.

3. In pouring a very thin section test casting it was found that the addition of 0.2 per cent aluminum gave optimum results in filling the mold and in the casting

surface. 4. Metal with no aluminum and poured at the highest temperature did not fill the mold completely, while castings with high aluminum and poured at the lowest temperature, out of the same crucible, completely filled the mold and formed considerable flash,

castability of yellow brass.

5. Aluminum additions up to 0.2 per cent gave incomplete thin oxide films which conceivably could be trapped in the metal and cause some trouble. Aluminum additions in excess of 0.2 per cent apparently gave heavier oxide films of a silvery color, which again could be more easily trapped in the metal.

indicating that aluminum has a strong effect on the

6. The observation that 0.2 per cent aluminum gives optimum results confirms the practice of many foundries which have found by experience that adding this amount aluminum will produce good yellow brass

castings with a pleasing golden color.

7. It was not possible to evaluate the effect of aluminum on finishing yellow brass castings. It is quite possible, however, that any aluminum added to yellow brass can produce oxide films which may remain entrapped in the metal and interfere with machining and polishing operations.

8. With the possibility of aluminum oxide film formation due to the use of aluminum additions, it appears necessary to make a careful selection of gating systems for yellow brass castings. In order to obtain the beneficial effects of aluminum in yellow brass, it appears essential to use non-turbulent gating methods which bring the metal into the mold quietly and do not agitate it unduly. By such methods it should be possible to obtain the advantages of aluminum and minimize the risk of such additions.

9. To establish close controls over the aluminum content of yellow brass castings the use of aluminumfree ingot is indicated. If unknown amount of aluminum is present in the ingot it is impossible to obtain consistently uniform castings; also, undesirable aluminum oxide films are apparently found more often when the original ingot material contains appreciable quantities of aluminum than when the only aluminum present is that added in the foundry.

DISCUSSION

Chairman: WM. ROMANOFF, H. Kramer & Co., Chicago. Co-Chairman: G. K. EGGLESTON, Barnes Mfg. Co., Mansfield, Ohio.

Co-CHAIRMAN EGGLESTON: When I first became associated with the foundry industry some 25 years ago, the foundry I was with was experimenting in plumbing fixtures made of yellow brass. We finally gave up the idea of using yellow brass. While we made good appearing castings they would not hold water. So since those early days I avoided yellow brass containing aluminum.

A. K. Higgins: 1 We started out about the same way, making yellow brass without aluminum, with all the attendant smoke and fumes, which was our principal concern. Then we got into the practice of using aluminum. We pour very few castings that we have trouble running because our sections are heavy. We arrived at our upper limit of aluminum by the simple change in the appearance of the metal as was described, from a yellow to a silvery appearance and automatically stopped at 0.2 per cent. I am interested to know that we got the same range by a quite different process and we have had satisfactory results with it. G. P. Halliwell: ^a I agree with him.

CO-CHAIRMAN EGGLESTON: I do too.

Mr. Colton: Can pressure-tight castings be made with this alloy? I am interested in this phase of the problem.

CHAIRMAN ROMANOFF: Pressure-tight castings have been made from manganese bronze, which is glorified yellow brass. It is difficult but it has been done. It is not recommended.

Mr. Colton: My feeling in the matter is that someone probably said once that pressure-tight castings could be made from this alloy. The effect of aluminum in yellow brass is mechanical, not metallurgical. There is no metallurgical reason why you cannot make sound castings with yellow brass with aluminum added providing the gating system is nonturbulent. The principal hazard would be possible entrapment of oxide films as with aluminum bronze which would prevent fusing of the metal.

G. E. Dalbey: 8 I would like to comment on the effect of aluminum and our particular experience with manganese bronze. We made pressure-tight castings in manganese bronze, but to do so requires a complicated, well designed system of gating, one which produces no turbulence. It can be done but it is difficult.

I would like to ask about the test for entrapment of the film, the authors bend test. I noticed the bend test film showed up parallel to the long axis of your plate, and you bend transverse to the long axis. If the author bent it the other way would it not have shown up the film more prominently?

Mr. COLTON: We gave up the bend test because it would be a matter of coincidence if the film, or cold shut, or misrun, call it what you may, had the right location, so that when you bent the test casting, the defect would show up. In this particular case it did show up in bending. I agree with you that if we had

Supt., Allis-Chalmers Mfg. Co., Milwaukee, Wis.
 Director of Research, H. Kramer & Co., Chicago.
 Metallurgist, Mare Island Naval Shipyard, Vallejo, Calif.

bent the specimen in other directions, the film might not have shown up at all. I tried polishing and several other methods, but it was difficult to establish any technique which gave reproducible results.

E. M. SMITH: 4 I would like to ask the author what experience he had remelting gates and risers? Does the melt come out fairly clean of dross? Is there quite a bit of skimming necessary?

MR. COLTON: How do you control aluminum in yellow brass? I really do not know. If you try to maintain no more than 0.2 per cent in your castings you find that when you remelt gates and risers you lose some aluminum. Some will oxidize out. Unfortunately we could never get a consistent loss. Some does come out. You would have to develop a technique to keep the aluminum at 0.2 per cent maximum by making a smaller aluminum addition. In other words the runaround might amount to 50 per cent of the charge, but if you have 0.2 per cent aluminum and lost none of it would have to add 0.1 per cent aluminum. If you lost no aluminum, and I did not add any to the melt, I do not think you would realize the beneficial effects of aluminum. My experience has been that the beneficial effects are most readily attained when you add aluminum to the metal just before you pour. Aluminum already present in yellow brass melted down does not seem to have as strong an effect. When it becomes alloyed I do not think you get the same amount of oxide film that you do when you add it. This constitutes no particular problem in many hardware shops. They add aluminum to every melt and apparently with their furnace conditions it works out well. Theoretically you can get into considerable trouble because of aluminum buildup. One of the hazards in using aluminum in yellow brass is this problem of control of

D. F. TAYLOR: 6 We have very little difficulty with any other alloy except yellow brass. In our shop we make clock cases which have to be perfect. In these castings appear to be small inclusions when made from commercial yellow brass ingot. What causes these inclusions?

Mr. COLTON: Your problem of inclusions in the metal has nothing to do with the aluminum content. The majority of yellow brass castings made in hardware shops today are made from commercial ingot. There are several kinds of ingot yellow brass. You can get commercial ingot without aluminum. Some smelters go to the expense of taking out aluminum which the foundryman can either add or not add at his discretion. That is one way to control aluminum content.

If you start with material that has an indeterminate amount of aluminum and you add aluminum to it you do not know how much aluminum you have. It will vary from one pot to another or from one furnace to another. I think it is possible to make sound castings with yellow brass containing aluminum but you must be aware of what you are doing. I agree that just any brass ingot will not make fine yellow brass castings.

MR. DALBEY: Would it be possible to cast a small test casting prior to pouring your real casting and using the surface of the test casting as an indication of how much aluminum you must add?

Mr. Colton: I think from the viewpoint of color it would be possible because there is an unmistakeable color change from 0.062 per cent to 0.16 per cent. I am not sure that would have any great effect in the casting because it is a matter of the gating

too. This casting is not well gated intentionally because I wanted to produce as much of this film as I could. You might make beautiful test castings and poor production castings because the gating would vary. From the color you could probably make a rough analysis of the aluminum content. I do not know how accurate it would be or how reproducible. It is a possibility. What do you do if the color indicates too much aluminum?

MR. DALBEY: We pig the metal and save the molds.

W. M. Ball, Jr.: In answer to Mr. Taylor, from his description his difficulty I believe is in his method of gating. His trouble arises from turbulence of the metal if small specks or dross show up on polishing of the castings. He will have to pour the metal more slowly so that the metal will flow in the mold more quietly to eliminate the inclusions he described.

MR. COLTON: I would like to point out that these inclusions might be sand grains trapped in the metal.

Mr. Ball.: You can always determine whether the inclusion is sand or oxide as they look entirely different.

Mr. COLTON: That is true. You can usually distinguish between the two.

MR. TAYLOR: The analysis of the ingot metal supplied to me is as follows: Cu-71.20; Sn-1.18; Pb-2.77; Zn-23.82; Ni-0.72. All these elements add up to 99.69. There is 0.31 per cent unaccounted for. If this was aluminum would it be enough to cause trouble? The holes in these castings were extremely fine and showed up only in the final polishing operation. They are not visible when the casting is machined.

Mr. BALL: It can be pinhole porosity, an oxide, or sand. These are three things you can have. You must first determine which one of these three conditions you have.

MR. TAYLOR: I have been working on the problem and I think I have eliminated the inclusions. I think it was an oxide.

MR. COLTON: With regard to the composition which you quoted, the analysis probably was made using wet methods. The chemist did not analyze for impurities, and took zinc by difference. I doubt that the remaining 0.31 per cent is all aluminum. If this were the case you could see it on an ingot. You do not really know how much aluminum is present. There are no good standard analytical techniques for aluminum, but there are experimental techniques as described in this paper. They are not available to everyone at the present time. I could account for the difference in analysis with the iron and antimony in yellow brass. The total of these two common impurities could very easily make up the difference in analysis. Aluminum can usually be detected on the surface of an ingot. You do not have to analyze for it. You know it is there.

Mr. TAYLOR: Is it a smooth top or a rough top? That is all you have to ask.

H. J. ROAST: Mr. Taylor must concentrate not only on the question of aluminum content but on other factors as well. He pours the castings in green sand molds. We are dealing with a variable question of moisture and all the numerous things that have been heretofore published and can be read in the A.F.S. Transactions. He must consider all these other points. It is only by a careful trial and error, changing only one thing at a time that his trouble will be found and presumably corrected. We must remember that he makes watch cases. We are passing into a field which to many of us is much more refined. For example, we might take castings and polish them and they might be perfect to us but when you get to the watch case it is a different proposition.

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PATTERN MATERIALS AND PRODUCTION IN PRECISION INVESTMENT CASTING

By E. I. Valyi*

ABSTRACT

Pattern materials for precision investment casting are reviewed. Methods of modifying wax compositions are given. Development of pattern plastics is described and data presented on comparative physical characteristics of pattern waxes and plastics. The use of metal patterns is reviewed.

Introduction

Precision investment casting is distinguished from other foundry methods by the use of perishable, instead of permanent patterns. The care and effort responsible for accuracy and surface appearance of the casting are, therefore, expended in producing the pattern, rather than the mold. The refractory mold surrounding perishable patterns is by no means without problems of its own, yet these problems are largely independent of the nature of the individual casting and of its geometry.

Thus in precision instrument casting, pattern production compares to sand molding plus coremaking while the refractory mold problem is more skin to sand preparation, blending and handling. The present discussion will deal with questions encountered in the selection of suitable perishable pattern materials and with methods of producing the patterns.

General Conclusions

The functional property requirements of a pattern material are as follows:

It must be moldable;

It must be dimensionally stable until a refractory mold has been formed around it;

It must be capable of elimination from the refractory mold.

A great number of available substances answer this description.

In the order of their occurrence in industry through the years, the following grouping of pattern materials suggests itself:

- I. Waxes, natural and synthetic
- 2. Low fusing alloys
- 3. Plastics
- 4. Frozen mercury (and other liquids)

Closer consideration of the problem demands, as may be expected, that the properties be defined in terms of measurable quantities. Some of these will be referred to in the subsequent discussions.

Above all, it will be attempted to show that it is not too difficult to find methods that will convert a multitude of the substances falling within the above wide categories into useful pattern materials. Nor is it hard to devise procedures for the economical production of patterns from any given group of these materials.

Since a great deal can be accomplished within the intrinsic property limits of any such materials group, the ultimate test becomes one of production economy, as represented by tooling cost, materials cost and molding cost.

Waxes

Compounding of pattern waxes, same as a number of other phases of precision casting, has for a long time been considered a trade secret of and guarded by suppliers to dental technicians and jewelry manufacturers. Literature on waxes used for precision casting patterns is practically nonexistent. In fact, literature on waxes in general, with the few exceptions of certain synthetic waxes used in the printing paper and textile industries, etc., is mostly descriptive and not sufficiently specific. There exist general books on wax technology such as, for example, books by A. H. Warth¹ and H. Bennett.² Perusal of these books is recommended to those interested in identifying any of the constituents of wax blends frequently encountered.

Except in the more or less related field of dentistry, none of the sources give quantitative data pertaining to characteristics important in precision casting pattern work.

Review of the few papers on the subject, such as R. Neiman,³ G. Vennerholm and E. Ensign,⁴ and others, but above all oral discussion and examination of samples, disclosed in short order some 30 different wax blends, composed of close to 40 various and sundry ingredients.

A representative number of these blends was obtained or prepared and tested by injection molding and through actual use, all under conventional production conditions.

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^{*} President, A.R.D. Corp., New York.

Following these tests, it was concluded that all of the blends are just about equally useful and quite expensive. Differences in molding shrinkage, strength and other properties were found, to be sure, but nothing was discovered that would have made any one of these numerous blends unsuitable for use in precision casting.

In view of this situation, it was decided to survey the field experimentally more from the standpoint of improvement of cheap wax blends than from the standpoint of new compounding techniques or new wax compositions, as the latter field seemed more than

adequately covered.

Several properties that are of importance in precision casting use were found in these pattern waxes, such as lack of residue on burn-out, reasonable lack of tackiness and stability of the blend at molding temperatures. It was, of course, taken for granted that any modification of waxes should result in no deterioration in these respects, nor harm the good molding characteristics usually available.

On the other hand, adequate strength and stability, as manifested by deformation and creep at room temperature and the recovery of such deformation, after removal of the load or restraint on the pattern, were

lacking in all waxes tested.

Improvement of Waxes

It was, therefore, decided to investigate what principle of modification would lead to improvement of most any given wax blend, resulting in "up-grading" of some of the inexpensive grades of wax, perhaps not even considered for precision casting use.

For this purpose, two exploratory studies were undertaken:

1. Improvement of any random wax or wax blend by "mechanical" reinforcement, through the addition of carbon black.

2. Improvement of paraffin waxes by blending with synthetic long chain paraffins, i.e., polyethylenes.

Since the lack of dimensional stability at and around room temperature and under small loads was the general defect to be corrected a simple test was decided on to provide quantitative data.

A cantilever deflection tester was built for the pur-

pose, as shown schematically in Fig. 1.

A molded test bar measuring ½ x ½ 5 in. is clamped at one end between the frame and a cover plate, leaving a cantilever on which deflection under various constant loads can be measured. At a distance of 3 in. from the support the deflection is registered by means of an Ames gage. The force applied is that of the thrust of the Ames gage and can be increased by adding weights to a platform provided for the purpose. Since the force applied by the gage alone varies slightly with the dial reading, a calibration curve was made for the load. In the presence of additional weight, the variation of the force was relatively negligible over a wide range of deflection. The tests were conducted in a constant temperature room (controlled within 5 F) at constant load.

The test bars were subjected to loads of 200 gm and 500 gm at several temperatures within the range of normal "room" conditions.

The plot of deflection versus time is not convenient to read, but a log log plot yields straight lines, at least in the range of practical interest, which is that of small loads and short times (5 sec to 2 min and more). In that range of the tests, the following relationship holds:

 $d = a \times t^b$ where d = deflection

t = time

a,b = characteristic contents of the sample

Since $\log d = \log a \times b \log t$, the characteristic constants can be read in the $\log \log p$ plot, b being the slope of the straight line, and therefore a measure of the relative rate of creep with time and a being the deflection after 1 sec and therewith a measure of the initial deflection.

The following data obtained were evaluated according to the log log plot. Some typical results are tabulated below to indicate examples of the general trend:

| | | | | n Rate of | | | Recov- |
|---------------|-------------|----|------|----------------------|-------|----|--------|
| Sample No. | Load, Gr | | | Flow (b) mils/sec | after | | |
| 1 | 200 | 78 | 13.9 | 0.155 | 28.4 | 70 | 80 |
| 1 | 500 | 78 | 38.5 | 0.160 | 79.0 | 79 | 88 |
| 2 | 200 | 78 | 8.7 | 0.190 | 21.0 | 68 | 77 |
| 2 | 500 | 78 | 25.6 | 0.170 | 56.0 | 78 | 88 |
| 3 | 200 | 78 | 8.7 | 0.135 | 16.2 | 65 | 70 |
| 3 | 500 | 78 | 24.1 | 0.125 | 43.0 | 85 | 93 |
| 4 | 200 | 77 | 4.7 | 0.49 | 45.0 | 26 | 36 |
| 4 | 200 | 84 | 9.8 | 0.56 | 116.0 | 20 | 27 |
| 5 | 200 | 77 | 8.2 | 0.17 | 18.0 | 63 | 79 |
| 5 | 200 | 84 | 15.2 | 0.25 | 48.0 | 58 | 70 |
| 6 | 200 | 77 | 10.8 | 0.16 | 22.6 | 66 | 74 |
| 6 | 200 | 84 | 17.5 | 0.18 | 40.0 | 64 | 76 |
| 7 | 200 | 77 | 18.7 | 0.13 | 34.0 | 68 | 83 |
| 7 | 200 | 84 | 28.0 | 0.15 | 56.0 | 67 | 76 |

These and other results in the same series of experiments (omitted here, since they would add little to the overall picture), shown in part in Fig. 2, permit the following interpretation.

Samples:

No. 1—Commercial base wax (not pattern grade) with 0% carbon black (tire reinforcing grade)

No. 2—Commercial base wax (not pattern grade) with 10% carbon black (tire reinforcing grade)

No. 3—Commercial base wax (not pattern grade) with 20% carbon black (tire reinforcing grade)
No. 4—Hard paraffin (m.p. 60 C) with 0% Bakelite polyethy

lene D 55 No. 5—Hard paraffin (m.p. 60 C) with 5% Bakelite polyethy-

lene D 55 No. 6-Hard paraffin (m.p. 60 C) with 10% Bakelite polyethylene D 55

No. 7-Hard paraffin (m.p. 60 C) with 10% Bakelite polyethylene DEM 1865

Carbon black reinforced waxes—The large deflection during the first second decreases markedly by the addition of the first 10 per cent of carbon black; further 10 per cent have little or no influence; a, of course, varies with the load applied and with the temperature.

The creep rate is not much affected by the presence of carbon black; b varies little with the load applied.

The recovery at the low load is deteriorated by carbon black; at the high load 10 per cent carbon black ıt

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carlack has no influence while 20 per cent carbon black improves the recovery. The influence of carbon black on recovery varies considerably from wax to wax.

(Note: Not too much must be concluded from such recovery values since they were observed after the same time under load for each sample, not after the same per cent deflection.)

Polyethylene reinforcement—The large initial deflection, a, which was markedly improved by the addition of carbon black, is increased by the first 5 per cent of polythene but not much affected by further addition. Offsetting this, the recovery, which was adversely affected by carbon black, is improved by polyethylene. This change again is not proportionate to the amount of polyethylene added. These two effects would indicate that polyethylene increases the elasticity of the wax.

The creep rate, b, is decreased substantially by the addition of polyethylene. This improvement outweighs the deterioration with respect to initial deflection, as the total deflection after 100 sec shows, which decreases with increasing polyethylene content, especially at the higher temperature. It should be kept in mind that part of this 100-sec deflection is reversible as soon as the load is taken off.

Samples 6 and 7 differ in the molecular weight of

AMES GAUGE

CLAMP

SAMPLE

Fig. 1-Deflection creep tester for pattern materials.

the polyethylene, No. 6 containing a polymer of five times the chain length. The shorter polyethylene causes a slightly greater improvement of the creep rate but increases the initial deflection so much that the overall effect after 100 sec clearly shows the superiority of the higher molecular weight. The slightly better recovery of sample No. 7 cannot balance the large initial deflection.

General Conclusions—Both carbon black and polymers like polyethylene improve the mechanical properties of pattern waxes at room temperature markedly. The improvements are brought about by different mechanisms.

Comparing the greatest improvement induced by any carbon black reinforcement in these tests with that obtained by any polyethylene addition investigated, the following may be seen.

The total deflection after 100 sec under 200 gm load of 20 per cent carbon black-reinforced wax was 16.2 mils, whereof 70 per cent were recovered; i.e., a permanent deformation of 4.9 mils had been caused. The unfilled wax had acquired a permanent deformation of 5.4 mils. Therefore, the addition of 20 per cent carbon black had improved this particular deformation by 9 per cent.

The total deflection of the same size test bar at the same temperature after 100 sec under 200 gms load of a 20 per cent polyethylene containing wax was 18.5 mils, whereof 87 per cent were recovered; the permanent deformation was 2.4 mil. The wax, under identical conditions, had acquired a permanent deformation of 32.5 mils. The addition of 20 per cent polyethylene had improved this particular deformation of a poor wax by 92.5 per cent.

Since carbon black improves a wax by depressing the a value, while polyethylene depresses the b value and both improve the recovery, a wax-carbon-blackpolyethylene blend shows potential advantage.

It is, in the meantime, reasonable to assume that a paraffin-10 per cent polyethylene blend performs better than most available pattern waxes, at greatly reduced cost.

It will be understood that all the samples tested in the fashion described were also molded and used to produce castings. Injection molding and elimination from the refractory mold proceeded with full success in all cases investigated.

Plastics

In a chemical sense one is, on occasion, hard put to state where waxes end and plastics start. Instead of an involved set of definitions, it is best to rely on common sense and "feel" in the present instance.

Such observation by touch and view reveals, of course, that there is a vast difference in strength, in resistance to damage and in potential accuracy between waxes and the multitude of combustible organic substances termed plastics, from which a wide variety of functional and decorative objects are made.

While the methods of reinforcement briefly referred to in the previous section improve waxes considerably, such improvement nowhere approaches the physical properties of plastics.

It was therefore obvious to attempt using plastics

in place of wax, as the industrial use of the precision investment casting method came about at a time when the chemistry and technology of plastics was highly developed. Unfortunately, however, it appeared that the conventional thermoplastic molding materials, while easy to mold and to handle, did not produce satisfactory refractory molds, such molds showing cracks and frequently surface defects. In addition, these conventional injection molding plastics required the use of comparatively expensive machinery and even more expensive tools. The latter constituted an important drawback in view of the fact that most of the objects considered for precision casting were extremely complicated and frequently, as in the case of gas turbine blades, not sufficiently set in design to warrant the expenditure required in connection with steel injection molds.

It is not surprising that the first attempts with injection molding plastics should have turned out in this fashion. After all, the materials that were so tried for precision casting patterns were originally developed for different purposes altogether. Usually, it was the aim of plastic manufacturers to produce a molding powder that results in strong, mar-resistant and comparatively heat resistant products, not to mention electrical properties and decorative effects. As it happens, the molecular structure resulting in such properties makes it intrinsically impossible to obtain at the same time such characteristics as would appear desirable from the standpoint of precision casting use.

In view of this, extensive research work was necessary to develop a pattern plastic, with that purpose alone in mind.

In an attempt to list such properties and characteristics as seem important, it is well to line up at the same time the difficulties that are encountered in meeting these requirements with normally available injection molding plastics.

- 1. Cost of tooling, cost of molding equipment, and cost of operation of the molding equipment should be low. Conventional plastics require steel dies and high pressure injection molding machines operating at high temperatures. It stands to reason that the expense incurred on all three counts is attributable to the comparatively high pressures and temperatures required in molding. Thus the advantageous employment of low-fusing alloy cast dies, such as possible with wax, is no more possible; equipment cost, as well as operating cost, particularly for large patterns, exceeds by far the amount required in connection with wax molding.
- 2. Accuracy of molding is more important in precision casting work than in conventional injection molding, since the normal product of injection molding operations is usually decorative, or otherwise dimensionally unimportant, whereas the very essence of precision casting is maintenance of close dimensional tolerances. The injection molding industry, with few exceptions, has paid little attention to the problem of molding accuracy. Consideration of the problem shows that normal injection molding resins as molded on conventional injection molding presses, lack accuracy mainly due to residual stresses in the molding which, in turn, are caused by the typically nonuniform

rate of expansion and contraction of such plastics and by elastic after effects (memory). The latter manifests itself in that a given sample of plastic of a certain geometric configuration will have a tendency to regain its original shape even after having been subjected to severe deformation.

3. A precision casting pattern should be stable in storage and in the course of the investing operation. Usually this requirement is readily met by the normal injection molding plastics but not so easily by resins that could be developed to overcome some of the aforementioned difficulties. For this reason, consideration of properties such as creep and creep recovery are still of considerable importance, as indicated in connection with waxes.

4. The pattern has to be inert to the liquids used in the preparation of molding refractories. Some of the conventional injection molding resins show swelling and surface damage upon exposure to water or alcohol, both possible vehicles in investing.

5. The behavior of the pattern during drying of the mold is of the greatest importance. Conventional injection molding plastics are characterized by an expansion curve that shows a vastly accelerated rate of expansion during and slightly above such temperatures as are usually employed in drying molds. In addition to this change in expansion rate, the same temperature range will relieve residual stresses in the pattern with a resulting tendency to warp and otherwise deform the part. Such strong deformations as will occur due to stress relief and thermal expansion will tend to deform and crack the mold.

6. It is important that the patterns should be capable of burning out completely in the course of firing. Most of the injection molding resins would satisfy this requirement providing inorganic admixtures, such as pigments and the like, are avoided. At the same time, one must not use plastics that would cause explosive burning.

7. Finally, price is an important consideration. It is not hard to meet the price level of the normal pattern waxes but, even so, a reasonable selection among plastics has to be made so that the material ultimately selected is one available without limitation at low cost.

Selection of a Family of Plastics

Because of their ease in molding, thermoplastics suggest themselves in preference to thermosetting resins. In addition most of the latter are not desirable because of the noxious breakdown products that form in the course of their combustion.

In view of the great variety of available thermoplastic materials, only a general indication of trends will be given here.

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It is reasonable to assume that not only one but several basic types of known thermoplastic resins can be employed upon suitable modification of structure and composition.

Before proceeding to evolve such suitable polymers, a selection on general principles as to the group or family of plastics had to be made.

The following general considerations apply.

Water-soluble, or even inadequately water-repellent

plastics will be ruled out; thus methyl-cellulose, for example, could not be used and cellulose acetate would not appear particularly desirable.

Plastics that are nonrigid at room temperature could hardly be used. This would rule out polyvinyl acetate, polyacrylates, and the higher esters of methacrylic acid

(ethyl, propyl, isopropyl-methacrylates).

Another group of plastics must be ruled out because they develop toxic vapors upon combustion and in addition turn out to be self-extinguishing when ignited which means that the burnout cycle would progress only with difficulty. This category includes vinyl chlorides, vinyl chloride copolymers, and vinylidene chlorides.

Plastics that will burn explosively will also have to be ruled out such as, for example, cellulose acetate.

On the other hand, a number of basic groups of plastics remain that could do good service in connection with precision casting: ethyl-cellulose, polymethyl methacrylate, polyethylene, mixed cellulose esters, such as cellulose-butyrate, and polystyrene.

Of these, polystyrene excels by its low cost, ready availability, excellent molding properties, water repel-

lency, and stability.

Development of a styrene polymer that does not show the disadvantages of conventional molding grades of polystyrene, tailored to the needs of the precision casting industry, will be specifically described in the following: it will be borne in mind that substantially the same basic considerations apply in adapting any of the others mentioned in the last group. Thus, the remarks that follow, while referring to polystyrene, are widely applicable to the other plastics as well.

Development of a Pattern Plastic

The structural characteristic responsible for a given amount of strength, creep rate, extrusion rate, also for applicable molding temperatures and pressures, is the molecular weight. Molecular weight, in case of these polymers indicates the chain length which, by virtue of its geometry and structural forces dominates response to external forces. For high strength, generally expected of conventional molding plastics, these materials are polymerized in a fashion to produce high molecular weights. While from the standpoint of molding alone a lower molecular weight plastic appears more desirable, it must be avoided in conventional applications because of the corresponding low strength. In case of precision casting patterns, the strength attainable at low molecular weight levels is still more than adequate and, therefore, it is reasonable to employ resins of low molecular weight for the purpose in order to obtain the superior molding qualities that are possible, in this instance, at greatly reduced pressures and temperatures of molding.

Volume expansion of these chain polymers in turn does not depend on molecular weight to an appreciable degree within the technically reasonable range of molecular weight variations. In order to influence the shape of the expansion curve one must add materials to the basic polymer that are compatible with it and will, at the same time, modify the expansion curve in a desirable fashion. It turns out that the additions that have such fayorable influence also help to over-

come the one difficulty encountered in molding low molecular weight resins generally, namely their brittleness. Some very low molecular weight resins alone may occasionally be so brittle that no molding around cores or intricate contours can succeed without breaking the part upon cooling in the mold.

The molecular weight level and the addition agents together can minimize the residual stresses in the molded article, providing the temperature range of cooling in the mold and the expansion-contraction characteristics of the resulting plastic are suitably

chosen in the first place.

It was possible to develop a plastic along these lines and to overcome certain difficulties that manifest themselves in the course of manufacture of such plastics so they are now available in quantity.

Properties of Pattern Plastic

A styrene polymer pattern plastic developed along these lines, exhibits the following properties that are of importance to the precision caster. (Description of the general range of mechanical properties will be omitted since they are fundamentally of no significance.)

In order to evaluate the rather elusive quality of moldability, extrusion tests were made with the aid of the Nason rheometer. That device measures the quantity of sample extruded in a predetermined time interval through a standard orifice at controlled pressures

and temperatures.

Figure 3 shows a comparison of extrusion rates obtainable, in comparison with a commercial grade of polystyrene. To further illustrate the point, Fig. 4 shows a somewhat approximate and safe presentation of recommended molding pressures and temperatures. It will be seen that several possible combinations of pressure and temperature fall within the range of equipment generally used for wax only. In the case of large patterns, the advantage of such behavior is obvious as the clamping pressure of the machines re-

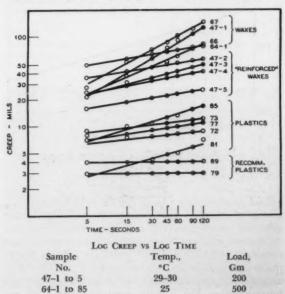


Fig. 2-Deflection of various pattern materials.

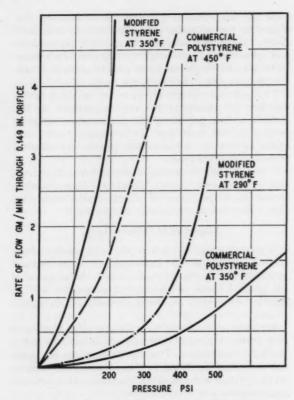


Fig. 3—Comparison of extrusion rates of commercial polysterene and the recommended plastic or modified styrene.

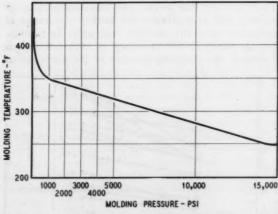


Fig. 4—Injection molding conditions for pattern plastic.

quired to mold such plastic can be held down to $\frac{1}{10}$ of clamping forces generally employed in the injection molding industry. If conventional injection molding equipment is available, temperatures in the neighborhood of 250 F with pressures around 10,000 psi are recommended. If low pressure equipment is available or new machines are to be chosen for the purpose, then 350 F with approximately 1000 psi should be used.

Deformation behavior of the plastic is shown in Fig. 2 with data on wax.

Perhaps the most important property of a pattern plastic is its expansion characteristics. It is well known that polymers of the general category used in injection molding exhibit a pronounced change in expansion rate at a well defined temperature. That temperature is customarily denoted as the second order transition point, as it represents the "point of transition," or rate change, of the second derivatives of thermodynamic variables.

As it is no more possible to do away with the second order transition point than to eliminate thermal expansion altogether, the only possible solution to the problem appeared to be such modification of the material as would result in depressing the second order transition temperature to room temperature.

With that expedient there is no sudden increase in expansion rate at any temperature that is likely to occur in the course of storage, molding, mold drying and firing. This linear expansion characteristic has been accomplished as shown in Fig. 5 which again is compared with a conventional molding grade of polystyrene.

Another characteristic that contributes to the uniformity of results in molding, is noted in the study of the progress of degradation of this pattern plastic at elevated temperatures. It is well known that all high molecular weight molding resins will degrade when exposed to temperatures such as required for the molding operation, particularly in the presence of oxygen. This fact is best exemplified by discoloration and loss of mechanical properties in plastics whenever they are in the heating chamber of a molding machine anything but the shortest possible time. Structurally, such degradation manifests itself by breakdown of chain length or, in other words, decrease of molecular weight.

It turns out that, given an adequate length of time, (such as, for example, over 2 hr) at molding temperature, all grades of polystyrene molding materials degrade to approximately the same molecular weight, no matter how high the initial molecular weight of the material may have been. The pattern plastic falls, for all practical purposes, within that molecular weight

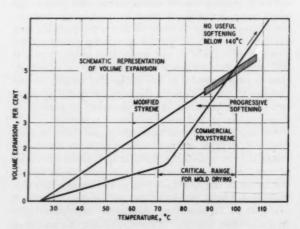


Fig. 5-Comparison of volume expansion.

range. Thus, prolonged exposure to temperatures not exceeding approximately 450 F will not affect its structure sufficiently to alter any of its significant expansion and extrusion properties. Therefore, re-use of scrap and gates is safe in any proportion. Should the precision casting procedure used call for draining of the molds, then again the material so drained can be safely re-used for molding without any particular preparation. Finally, this property allows the use of extremely simple molding apparatus in place of the usual torpedo-spreader type of heating chamber used in injection molding machines.

Pronounced breakdown, mainly by oxidation, sets in at temperatures exceeding 500 F, followed by true

pyrolysis at still higher temperatures.

Pyrolysis progresses rapidly at temperatures approaching 800 F to 850 F; it is possible to reduce the quantity of remaining solids to approximately 5 per cent of the original weight in 30 min to 1 hr at 850 F, the residue being carbon in a fluffy form. For complete burn-out, such residual carbon has to be oxidized. This can be accomplished under reasonably oxidizing conditions at 1200 F or above. In the course of burn-out no liquid diffusion into the refractory will take place. Gases of combustion will penetrate the mold body and progressively color it dark brown, gray, and black. The end of the firing cycle can be noted by the disappearance of the black color. Of course, no residue is left after burn-out.

Results

How do these properties reflect upon processing of this new material and the resulting patterns?

The low molding pressures and temperatures make it possible to use cast molds made substantially in the same fashion as the lead, cerro-alloy, or other low fusing alloy molds used in connection with wax. Of course, the alloy chosen for the molds must not melt below the molding temperature of the plastic. It was found that the customary grades of zinc die casting alloys make satisfactory molds and can be processed by methods applicable to lower fusing alloys, well

known to precision casters.

As has been mentioned before, low or high pressure equipment are equally suitable in injection molding. Even if automatic and highly productive molding methods are called for, the machinery may be comparatively light, even though capable of producing exceptionally large parts. Figure 6 shows injection molding presses designed for large scale production work that have molded patterns weighing up to 2 lb, representing no more than approximately one-third of their weight capacity per shot, or ring bodies measuring up to 18 in. in diam, yet the machines have a clamping capacity of only 50 tons. Compared to equipment that would be required for molding conventional thermoplastics, the use of these low pressure machines turns out to be extremely economical; as an example the machines shown require not over 10 hp whereas a conventional machine of equal weight capacity would have to be powered with no less than 60 hp.

Molding tolerances will, of course, depend on accuracy of mold and careful observance of proper molding cycle. Generally speaking \pm 0.0015 in. per in. is not

difficult to attain.

The patterns remain true to dimensions and are not affected by mechanical handling, storage and other loads that can reasonably be expected. Pattern assembly, prior to investing, is greatly facilitated by the mechanical strength of these patterns. Assembly operations can be carried out with the aid of solvents or by heat sealing, the latter carried out much the same as in the case of wax. Sprues and runners made of plastic are preferable though there is no reason why wax could not be used as long as care is taken not to trap any wax in back of the plastic pattern since otherwise mold erosion would occur due to violent boiling of the wax at mold drying temperatures, that are not yet high enough to cause the plastic patterns to flow out, but much higher than the wax melting point.

In drying and firing no mold cracking will occur as a consequence of the expansion characteristics previously described. It is not advisable to cause drainage of the plastic from the refractory mold to take place at comparatively low temperatures, since the viscosity of the plastic at such temperatures is still quite high and, therefore, the danger exists of dragging out delicate portions of the mold or unsupported cores. This can easily be avoided by not exceeding mold drying temperatures of approximately 160 F and by speeding the firing cycle up to 800 F as much as the mold refractory will permit. In this fashion, the pattern plastic will not have enough time to creep appreciably and to flow out of the mold under its own weight but will be brought to a high enough temperature for rapid breakdown before any such flow could have taken place.

While the pattern plastic has generally lower strength than commercial molding powders of the polystyrene family, the physical properties are still more than adequate as can be exemplified by the following data:

Tensile Strength, psi 6000

Elongation, per cent 2.40

Flexural Strength, psi 8900

Flexural Deflection, in. 0.186

Impact Strength, ft lb 0.37

(notched Izod)

Modulus in Tension, psi 4.03 x

Pattern Plastic Commercial Polystyrene
6000 8000 to 9000
2.40 2.5 to 3
8900 12,000 to 14,000
0.186 0.20 to 0.25
0.37 0.7 to 0.9

osi 4.03 x 10⁵ 4.03 x 10⁶



Fig. 6-Low pressure injection molding machines.



Fig. 7-Residual stress pattern of (Top) pattern plastic and (Bottom) commercial polysterene.

The comparison of residual stresses in moldings made from the pattern plastic as against commercial molding plastics, is shown in Fig. 7. It is obvious that the minimizing of residual stresses will provide dimensionally stable patterns that will not be influenced by changes in ambient temperature be it in storage or in the course of further processing.

Figures 8, 9 and 10 show examples of patterns and castings, the former molded from a styrene polymer, such as described, at approximately 350 F, using 1000 psi molding pressure.

Metal Patterns

Metal patterns came into view again, as of late, as a consequence of a recent development based upon the use of frozen mercury.5

When referring to metal patterns, the foundryman must be cautioned not to think that the discussion has departed from perishable patterns. Frozen mercury will best be dealt with under the broader heading of metal patterns.

It will be appreciated that certain metals and alloys, mainly the ones melting below say 400 F, or even 700 F, might have several of the properties expected in a

precision casting pattern material.

They are easy to cast into accurate shapes, using standard permanent mold or die casting techniques. They have strength and stability in storage and handling.

Generally, three definite problems have to be coped with.

1. Handling of molds—All metals that may be considered are much heavier than waxes and plastics. While the mechanical aspects of handling can certainly be overcome in the plant, the increased weight of molds constitutes a slight inconvenience. In the case of mercury, the handling problem is aggravated by the necessity of keeping the temperature of the pattern below freezing.

The mold and pattern handling problem is strongly influenced by the obvious need for removing even the last traces of the pattern from the mold by draining (as will be referred to later), without being able to rely on combustion in the course of the firing cycle, as with organic pattern materials.

Recovery of the pattern metal is mandatory, since otherwise cost would be much higher than may be tolerated. The metal drained from the refractory molds can be refined for re-use by conventional fluxing procedures.

All of these operations concerned with draining and recovery, add considerably to the bill of handling, compared to wax and plastic.

2. Thermal Expansion—It will be readily appre-



Fig. 8—Casting gate made from plastic pattern, approx.

10 lb pattern weight corresponding approx. 70 lb of steel.

ciated that high volume expansion of a metal pattern is apt to damage the refractory mold, particularly if such expansion is not accompanied by any significant loss of strength. For this reason, the pattern metal must be selected from the group of bismuth-bearing alloys that show either no concentration or even slight growth upon solidification. These alloys, generally known as soft metal or fusible alloys, include several eutectics in systems mainly containing Bi, Pb, Sn and Cd. The ones containing close to 50 per cent Bi generally exhibit no volume change upon melting.

Eutectic alloys are advantageous, since the mold produced around patterns made from them may be dried at a temperature just below the melting point and the pattern metal drained within a narrow temperature interval.

Mercury behaves in a similar manner, except for the fact that the temperatures are in a refrigerating, rather than in the heating range.

3. Surface Films—All of the otherwise fusible alloys, other than mercury, tend to develop surface oxide films, occasionally also more complex films, due to the interaction between pattern and mold refractory. These films have a tendency to adhere to the refractory mold wall, rather than to leave the mold with the bulk of the pattern metal. There are three methods available that aid in overcoming the difficulty: coating of the pattern with a suitable lacquer; coating with a flux, such as ammonium chloride; and plating with iron, nickel, or chromium, providing the plated deposit will not affect the ultimate casting adversely. Before any of these surface treatments is applied, the pattern surface will be degreased, or even etched.

Equally important is the need for not heating the pattern through the mold body, but directly. This is readily accomplished by flushing the pattern with molten metal from an outside source, so as to ease the



Fig. 9-Low carbon steel casting with plastic pattern.

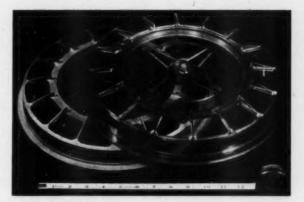


Fig. 10-Plastic pattern for stainless steel casting.

collapse of the pattern, minimize mechanical damage to the mold surface and reduce the chances of surface oxidation of the pattern.

With most of these difficulties overcome extensive use of metal patterns may be foreseen, even though practically no such use exists at present. Mercury patterns have been applied to some extent; it would seem however that operation at room temperature, lack of toxicity and production economy favor the use of bismuth-tin alloys, whenever metal patterns are to be considered.

A comparison of metal patterns with wax and plastic patterns would not be fair at this time, because all operating experience on a large scale is lacking. Considering the high efficiency of up-to-date die casting methods that would, no doubt, be applied to the production of patterns, favorable results may be anticipated.

Conclusions

Patterns for precision investment castings may be made from waxes, plastics and low-fusing alloys.

The cost of waxes suitable for such use may be reduced with simultaneous gain in quality, by reinforcement of comparatively low grade waxes and paraffins with carbon black, and more effectively, with polyethylene

In order to increase production economy and improve the significant properties by a wide margin over waxes, and to further reduce material cost, plastics are employed that deviate from the ones used in other industrial applications and thus had to be developed for this specific purpose.

Metal patterns, made from eutectic bismuth alloys show considerable promise but have not as yet been industrially applied. They tend to overcome some of the drawbacks of frozen mercury, which was thus far used on a limited scale with satisfactory results insofar as quality of ultimate castings is concerned, but not with regard to production cost.

At the present time, plastic patterns appear to have proven best, everything considered.

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DISCUSSION

Chairman: L. E. GRUBB, International Nickel Co., Bayonne,

Co-Chairman: T. E. MOORE, Ransom & Randolph Co., Toledo,

PAUL SOLOMON:1 Dimensionally and for ease of handling, it is easy to understand where plastic patterns would have an advantage over wax patterns. When dealing with customers and filling out quotation sheets, we have a die service cost to consider.

First of all, you have to have a large run of parts to amortize the cost of a steel die for plastic injections. The normal run of parts is usually anywhere from 1,000 to 5,000 pieces. If you quote with the cost of the steel die, the customer requesting his particular part is amazed at the expensive price per piece, and starts looking for some other way for making his part. Our practice has been to quote using low-fusing metal dies that make the price per piece as reasonable as possible.

You say that you are injecting plastic at approximately 350 F. This, naturally, prohibits the use of low-fusing metal dies. If you inject the plastic at 200 F into a soft metal mold, the pressure you would require to do this would be too great for

use with low-fusing dies.

I feel that many of the men in the field have avoided plastics because it is expensive and usually there is no economic justification for this expensive technique.

DR. VALYI: The die cast problem is, of course, a very im-

Naturally, one cannot inject plastic at a temperature that exceeds the melting point of the mold metal. It was shown, however, that the pattern plastic can be injected at 250 F providing a pressure of approximately 8000 psi is used. At that temperature, cerrotru molds may be employed since cerrotru melts somewhere near 290 F.

Unfortunately, not everybody has equipment within that pressure range and if the pressure is reduced the temperature has to be increased. Instead of using cerro-alloys other alloys may be used at only insignificant increase in cost such as, for example,

conventional zinc die casting alloys.

Compared to the conventional cerro-alloy wax mold, the zinc alloy molds may cost one-half up to twice as much which is still no where near the cost of a steel injection mold. There are, of course, alloys that may cover a temperature range between say

cerrotru and zinc die casting metal.

As to the question of using low-fusing alloys at high pressure, no ill effect has ever been observed. Reference may be made to certain wax molding equipment in which pressures of 10,000 psi or over have frequently been attained. The reason is seen in the fact that no actual pressure is exerted before the mold is completely filled, following which a hydrostatic pressure condition is set up so that the forces acting upon protruding sections of the mold are substantially balanced. This experience is borne out in all cases where plastic was molded in low-fusing alloy molds at high pressure.

Mr. Solomon: That would be true, depending upon the surface area inside the die. We have run tests injecting wax at various pressures. To illustrate, one particular part gave us trouble due to a shrink dip on a broad, flat surface. We eventually eliminated the dip at a pressure of 50,000 psi. At this extremely high pressure, the die did not hold up very long. We could get only approximately 100 patterns out of a cerro-true die, before the low-fusing metal started to flow and we eventually had to go to a steel die that was enclosed by a 3/4-in. steel tapered ring to hold the pressure within the die.

DR. VALYI: That is a different problem altogether relating to

clamping and retaining the soft metal mold on the outside.

Mr. SOLOMON: We have the problem of clamping that shows we have the problem of pressure within the cavity of the die.

Dr. VALYI: If you have proper clamping and prevent squeezing out of the soft die to the sides, then nothing will move inside the die cavity.

In general, I think that while plastic patterns may not replace everything else they will make certain precision applications possible that were not feasible with wax. I think there is economic justification for both wax and plastic in their proper places. As an example, the large ring pattern could hardly be made in wax.

MR. SOLOMON: Turbine wheels of that size are being manufactured by several companies in sections, then assembled in order to get away from the tremendous pressure required to make the wheel with one injection. In fact, that particular one you refer to is at present being manufactured by the techniques just mentioned, of injecting wax and assembling.

DR. VALYI: Using modern injection molding equipment, one can really make speed with a single shot set-up compared to an

assembly.

MEMBER: Does this plastic require investing technique other

than the common practice?

DR. VALYI: While I would rather not go too far into the investment problem since it bears no direct relation to the patterns discussed, I would like to point out generally that, in my experience, it makes little difference what the pattern is made of. The pattern expansion as shown is matched for molding materials containing substantially silica. The pattern plastic has been used in connection with the preceat technique and also with slip cast molds. It was used with plaster and plaster containing investment. Up to this time, I really cannot say that there is any need for special precautions due to the plastic.

From the standpoint of burn out, we must keep in mind that towards the end of the firing cycle we have carbon, whether we started out with wax or plastic. In attempting to obtain a clean burn out, one must try to get as little remaining matter to diffuse into the mold body as possible. There is one difference

between wax and plaster however.

While most of the wax drains out of the mold, plastic remains in the mold practically up to the point where it begins to decompose into carbon. Therefore, a larger quantity has to be oxidized within the mold. Well controlled oxidizing conditions must be maintained in the firing furnace and decomposition of the plastic can then be completed in a comparatively short time.

MEMBER: What are the economics in using plastic as compared with wax? When using the wax, we reclaim a considerable

amount of the wax. Can you reclaim the plastic?

DR. VALYI: Reclaiming of plastic is possible at the proper temperature, somewhere in the neighborhood of 450 F, but even then the plastic will not flow the way wax does. If the mold contains slender cores, particularly blind cores, there will be difficulty due to any attempt at plastic recovery as the plastic will have a tendency to break out such protruding mold portions.

CO-CHAIRMAN MOORE: If you had an order for 100 or 2000 average parts, would it pay to make that part of polystyrene instead of wax? I believe that if you had 20,000 or 50,000 there might not be much argument as regards paying for the polysterene pattern, but do you know of any quantity that might be feasible?

Dr. VALYI: I would say that plastic patterns are favored with high production quantities and wax for shorter runs.

While one can make zinc alloy molds at comparatively low expense and therefore produce plastic patterns even for short runs, there seems no good reason to do it. If there is a small quantity of parts required, such as 50 or 100, then it is usually a question of getting the castings out fast. There is no call for wanting to store patterns or waiting to put patterns through a production line set up where handling becomes a problem. I would, perhaps put it this way: If a shop is using plastic patterns primarily, then they may use plastic for low production runs as well; but if they have used both wax and plastic anyway then they will probably run quantities up to say 500 castings in wax, about 1500 in plastic, and will decide from case to case how to cover the range in between.

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PHENOLIC RESIN CORE BINDERS

EFFECT OF CORE COMPOSITION ON PHYSICAL PROPERTIES OF CORES

By

J. E. McMillan and J. A. Wickett*

THE OBJECT OF OUR LABORATORY AND FOUNDRY evaluation work was the development of a phenolic core binder resin suitable for foundry use. In order to develop a suitable resin, it soon became apparent that the resin evaluation work could be meaningful only when correlated with the effects of other components in the mix. Accordingly, one phase of our basic project became the study of the effect of core composition on the physical properties of cores. More specifically, we have attempted (1) to establish reproducible results in the laboratory, (2) to determine how these results are affected by core components, and (3) to correlate physical properties of core mixes with casting quality. It is with these objectives that we are concerned in this paper.

Experimental Work

Effects of cereal, resin and water on core properties have been investigated. It is realized that many other ingredients are commonly used and their effects on core properties are currently being investigated. If some properties can be shown to have specific relationships to core composition, it is reasonable to assume that relationships of other properties can be established. This report is part of a large program which has been arranged so that additional variables may be included as work progresses.

These initial core tests have been stripped to the bare essentials of sand, cereal, resin and water. The sand used was a washed and dried, round grained Ottawa silica sand. An average screen analysis (Table 1) for this sand was selected which gave the closest approximation to Standard Test Sand recommended for research purposes by the American Foundrymen's Society.

A commercially available cereal binder was used to produce green strength. Bentonite binders were avoided because they decrease the baked strength of resin bonded cores.

A series of phenol-formaldehyde resins of various types were screened by recommended standard core tests. (FOUNDRY SAND TESTING HANDBOOK, Sec. 15, 1944). A water soluble, liquid phenolic resin, was selected for the tests described in this report.

With these basic materials, core mixes of varying formulation were prepared. A scheme for representing these formulations was designed so that it indicated the order and amount of each component added to the sand. For example, a core mix designated as 1.5–0.3–3 was prepared by adding 1.5 per cent cereal (C) to the sand followed by 0.3 per cent resin solids (R) and 3 per cent water (W). The per cent of each ingredient was based on the weight of dry sand. Unless otherwise specified, this designation is consistent throughout this report. Resin additions were made in terms of resin solids rather than total resin for the purpose of comparing resins of various solids content.

The following limits of composition were tested in this program:

> C = 0.5 to 2.5 per cent R = 0.0 to 1.5 per cent

W = 1.0 to 15 per cent

TABLE 1-SCREEN ANALYSIS OF TEST SAND

| U. S. Series Equivalent No. | Per cent on Screen |
|--------------------------------|-----------------------|
| 20 | - |
| 40 | - |
| 60 | 6 |
| 80 | 82 |
| 100 | 12 |
| 140 | _ |
| 200 | - |
| 325 | |
| Pan | - |
| | |
| AFS Fineness No. | 61.2 |

TABLE 2—EFFECT OF MULLING TIME ON BAKED TEN-SILE STRENGTH SIMPSON LABORATORY MULLER

(Core Composition 1.0 - 0.3-5)

| Dry Mull Time, Minutes | Wet Mull Time, Minutes | Residual Moisture, % | Baked Tensile Strength,psi |
|---------------------------|---------------------------|-------------------------|-------------------------------|
| 2 | 4 | 4.5 | 342 |
| 2 | 6 | 4.3 | 327 |
| 2 | 8 | 4.1 | 326 |

^{*} Plastics Division, Monsanto Chemical Co., Springfield, Mass.

Twenty-five pound mixes were prepared in a Simpson laboratory muller. The sand, cereal and any other dry ingredients were mulled for 2 min; resin, water and any other fluid ingredients were added and the mixture then mulled for an additional 4 min. This mulling cycle was adopted when it was observed that wet mulling longer than 4 min did not increase baked tensile strength of the core mix as shown in Table 2.

After mulling the core mix was transferred to an air tight container.

The moisture content of each mix was checked on a Dietert moisture teller. The moisture represented in the compositions given denotes the total moisture added to the mix. Due to evaporation the actual moisture after mulling was generally 0.2 to 0.5 per cent lower than the calculated amount.

The retained sample of the core mix was tested the same day for green compressive strength, baked tensile

strength and surface hardness.

The green compressive strength of each core mix was determined on a Dietert spring compression apparatus. The baked tensile strength was determined by the A.F.S. standard briquet method. Five briquets were prepared from each mix; at least three independent core mixes were prepared for each composition discussed in this paper. They were baked at 400 F for 1 hr in a gas-fired, forced-draft laboratory oven. The briquets were cooled to room temperature in an air tight container. The baked tensile strength of these briquets was determined on a modified Tinius-Olsen machine at a loading rate of 575 lb per min. The average tensile strength of the core mix was calculated. Those values which deviated more than 10

EFFECT OF BAKING TIME
AND TEMPERATURE ON
BAKED TENSILE STRENGTH

C-R-W
IS-05-5

200

C-R-W
IS-05-5

FIGURE 1

15 30 60 120

BAKING TIME, MINUTES

per cent from the average were discarded and the average recalculated.

The initial tensile tests were attempted on both the Dietert and Tinius-Olsen machines. Since the baked tensile strength of some core mixes exceeded the range of the Dietert machine available, all tensile data reported were determined on the Tinius-Olsen machine.

After testing for tensile strength the broken briquet was reassembled and a Dietert scratch hardness determined on the surface exposed during baking.

Since it is general foundry practice to employ baking temperatures of 400 F and above, cores were tested at 400 F and 500 F for various time intervals up to 2 hr. Curves showing the typical effect of time and

temperature are given in Fig. 1.

The maximum baked tensile strength was developed after baking the core for 1 hr at 400 F. Extending the baking time to 2 hr did not appreciably affect the tensile strength. The same maximum baked strength is reached after 30 min at 500 F. Continued baking beyond 30 min produces a rapid decline in strength. Apparently this reduction is due to the "burn-out" of the cereal binder. The critical temperature at which cereal "burn-out" begins lies somewhere between 400 and 500 F. However, this does not mean that cores

TABLE 3-EFFECT OF COMPOSITION ON PROPERTIES

| Composition | Green Str | ength, psi | | Tensile gth, psi | Scratch Hardness Dietert Units |
|-------------|------------|-------------|----------|---------------------|---|
| C-R-W | Observed C | Calculated* | Observed | Calculate | d* Observed |
| 0.5-0-1 | 0.24 | 0.24 | 0 | 0 | - |
| 2 | 0.47 | 0.48 | 71 | 32 | 8 |
| 3 | 0.40 | 0.50 | 93 | 96 | 40 |
| 4 | 0.48 | 0.50 | 101 | 96 | _ |
| 5 | 0.47 | 0.50 | 97 | 96 | _ |
| 1.0-0-1 | 0.37 | 0.37 | 29 | . 0 | 6 |
| 2 | 0.54 | 0.49 | 48 | 32 | 17 |
| 3 | 0.48 | 0.61 | 115 | 104 | _ |
| 4 | 0.70 | 0.65** | 145 | 161 | 55 |
| 5 | 0.64 | 0.65 | 162 | 186 | _ |
| 6 | 0.71 | 0.65 | 182 | 186 | 63 |
| 8 | 0.69 | 0.65 | 185 | 186 | 70 |
| 1.5-0-1 | 0.47 | 0.58 | 0 | 0 | 0 |
| 3 | 0.71 | 0.74 | 136 | 120 | 52 |
| 4 | 0.75 | 0.76** | 177 | 161 | _ |
| 5 | 0.69 | 0.76 | 208 | 193 | _ |
| 6 | 0.76 | 0.76 | 217 | 239 | 72 |
| 9 | 0.75 | 0.76 | 239 | 239 | 83 |
| 12 | 0.75 | 0.76 | 242 | 239 | 87 |
| 15 | 0.83 | 0.76 | 244 | 239 | 89 |
| 2.0-0-3 | 1.2 | _ | 96 | 104 | 57 |
| 4 | 0.98 | | 145 | 161 | 68 |
| 5 | 0.88 | - | 180 | 193 | _ |
| 6 | 1.08 | | 187 | 224 | 80 |
| 8 | 0.89 | _ | 272 | 276 | 88 |
| 12 | 0.83 | _ | 275 | 276 | 93 |
| 2.5-0-3 | 1.41 | _ | 109 | 104 | 67 |
| 5 | 1.15 | _ | 174 | 193 | 73 |
| 2.5-0-6 | 1.34 | _ | 185 | 224 | 86 |
| 9 | 1.16 | - | 213 | 306 | 90 |
| 10 | 1.03 | desirate. | 314 | 306 | 95 |
| 12 | 1.05 | | 252 | 306 | 95 |
| 14 | 0.97 | | 306 | 306 | 100 |
| 15 | 0.90 | | 274 | 306 | 100 |

TABLE 4—EFFECT OF COMPOSITION ON RARE PROPERTIES

| Composition | Green Stre | ength, psi | | Tensile 5th, psi | Scratch Hardnes Dietert Units |
|-------------|--------------|-------------|----------|---------------------|--|
| C-R-W | Observed C | alculated* | Observed | Calculate | d* Observed |
| 0.5-0.1- 1 | 0.19 | 0.24 | 76 | 87 | 0 |
| 2 | 0.43 | 0.48 | 139 | 137 | 40 |
| 3 | 0.40 | 0.50 | 148 | 137 | 53 |
| 4 | 0.49 | 0.50 | 161 | 137 | 58 |
| 5 | 0.50 | 0.50 | 144 | 137 | 54 |
| 1.0-0.2- 1 | 0.30 | 0.28 | 70 | 87 | 25 |
| 2 | 0.46 | 0.41 | 167 | 174 | 58 |
| 3 | 0.47 | 0.49 | 254 | 261 | |
| 4 | 0.52 | 0.54 | 267 | 268 | 78 |
| 5 | 0.59 | 0.59 | 256 | 268 | 74 |
| 6 | 0.60 | 0.62 | 253 | 268 | 85 |
| 8 | 0.62 | 0.65** | 267 | 268 | 89 |
| 10 | _ | _ | 232 | 268 | 85 |
| 1.5-0.3- 1 | 0.38 | 0.35 | 41 | 87 | 17 |
| 3 | 0.59 | 0.56 | 246 | 261 | 77 |
| 4 | 0.64 | 0.61 | 370 | 361 | _ |
| 5 | 0.63 | 0.66 | 325 | 361 | 93 |
| 6 | 0.65 | 0.69 | 348 | 361 | 93 |
| 9 | 0.73 | 0.75** | 366 | 361 | 93 |
| 12 | 0.77 | 0.75** | 368 | 361 | 95 |
| 2.0-0.4-4 | 0.72 | _ | 363 | 348 | 90 |
| 8 | 0.62 | _ | 414 | 440 | 97 |
| 12 | 0.73 | | 460 | 440 | 99 |
| 2.5-0.5- 5 | 0.82 | _ | 432 | 435 | 98 |
| 7 | 0.91 | _ | 523 | 512 | 99 |
| 10 | 0.85 | _ | 530 | 512 | 100 |
| 12 | 0.84 | | 530 | 512 | 100 |
| * E1 | npirical Val | ues explain | ed in di | guations | |

cannot be baked above the critical temperature in actual foundry practice because there is normally a temperature lag and an isothermal condition existing in the core controlled by the unevaporated water. But to obtain a relatively nonfluctuating system for the purpose of studying comparative data, a baking cycle of 1 hr at 400 F was selected for these tests.

Ramming conditions used were those recommended by standard AFS tests, namely weighed quantities of core mix were used for both green and briquet specimens and given three rams on the Dietert rammer before testing. The properties reported here are believed to be the maximum obtainable from these ramming conditions. However, in recent experiments, beyond the scope of this report, it has been observed that the degree to which sands are rammed will greatly affect the green and baked properties of core mixes. This effect has been observed particularly in subangular sands. Higher and more consistent results will be obtained as the degree of ramming increases.

Discussion of Laboratory Results

Green Strength—The green compressive strength of cores (as listed in Tables 3 to 6) depends upon the proportions of cereal, resin and water in the core mix.

Initially, it was considered that green strength was a simple function of cereal and water concentrations. This has proven to be only part of the answer to green strength problems. At the same time, it was known that green strength data obtained in the laboratory was normally lower than that obtained from sands

TABLE 5-EFFECT OF COMPOSITION ON RARE PROPERTIES

| C/R = 5/2 | | | | | | |
|-------------|---------------------------|------------|----------|---------------------|--|--|
| Composition | n Green Stre | ength, psi | | Tensile (th, psi | Scratch Hardnes Dietert Units | |
| C-R-W | Observed C | alculated* | Observed | Calculate | d* Observed | |
| 0.5-0.2- 1 | 0.17 | 0.16 | 73 | 124 | 20 | |
| 2 | 0.29 | 0.29 | 154 | 159 | 49 | |
| 3 | 0.30 | 0.37 | 206 | 159 | 68 | |
| 4 | 0.41 | 0.43 | 205 | 159 | 70 | |
| 5 | 0.45 | 0.47 | 182 | 159 | 65 | |
| 1.0-0.4- 2 | 0.40 | 0.41 | 236 | 288 | 65 | |
| 4 | 0.44 | 0.54 | 313 | 312 | 87 | |
| 6 | 0.49 | 0.62 | 311 | 312 | 88 | |
| . 8 | 0.56 | 0.65 | 291 | 312 | 89 | |
| 10 | _ | - | 297 | 312 | 95 | |
| 1.5-0.6- 2 | 0.42 | 0.41 | 261 | 290 | 79 | |
| 4 | 0.44 | 0.54 | 382 | 356 | 88 | |
| 6 | 0.45 | 0.62 | 354 | 356 | 90 | |
| 8 | 0.60 | 0.65** | 355 | 356 | _ | |
| 10 | 0.55 | 0.65 | 348 | 356 | 95 | |
| 2.0-0.8- 4 | 0.63 | _ | 486 | 492 | 100 | |
| 6 | 0.67 | - | 527 | 528 | 100 | |
| 8 | 0.63 | - | 540 | 528 | 100 | |
| 12 | 0.65 | _ | 511 | 528 | 100 | |
| 2.5-1.0- 5 | 0.77 | _ | 568 | _ | 100 | |
| 7 | 0.77 | - | 565 | 621 | 100 | |
| 10 | 0.71 | _ | 640 | 621 | 100 | |
| 12 | 0.76 | _ | 617 | 621 | 100 | |
| | npirical Val aximum Li | | | | | |

TABLE 6-EFFECT OF COMPOSITION ON RARE PROPERTIES

| $C/R \equiv 5/3$ | | | | | | |
|------------------|--------------|------------|----------|---------------------|--|--|
| Composition | n Green Stre | ength, psi | | Tensile 3th, psi | Scratch Hardnes Dietert Units | |
| C-R-W | Observed C | alculated* | Observed | Calculate | d* Observed | |
| 0.5-0.3- 1 | 0.15 | 0.16 | 90 | - | 26 | |
| 2 | 0.31 | 0.29 | 210 | 181 | 61 | |
| 3 | 0.35 | 0.37 | 229 | 181 | 75 | |
| 4 | 0.44 | 0.43 | 228 | 181 | _ | |
| 5 | 0.46 | 0.47 | 218 | 181 | 73 | |
| 1.0-0.6- 2 | 0.42 | 0.41 | 261 | 290 | 79 | |
| 4 | 0.44 | 0.54 | 382 | 356 | 88 | |
| 6 | 0.45 | 0.62 | 354 | 356 | 90 | |
| 8 | 0.60 | 0.65** | 355 | 356 | | |
| 10 | 0.55 | 0.65** | 348 | 356 | 95 | |
| 1.5-0.9- 3 | _ | 0.56 | 426 | 434 | 90 | |
| 6 | 0.57 | 0.69 | 474 | 494 | 100 | |
| 9 | 0.55 | 0.75** | 458 | 494 | 100 | |
| 12 | 0.58 | 0.75** | 468 | 494 | 100 | |
| 15 | 0.67 | 0.75** | 456 | 494 | 100 | |
| 2.0-1.0- 4 | 0.66 | _ | 575 | 580 | 100 | |
| 8 | 0.61 | _ | 618 | 616 | 100 | |
| 12 | 0.60 | _ | 583 | 616 | 100 | |
| 2.5-1.5- 5 | 0.81 | _ | 686 | 615 | 100 | |
| 7 | 0.70 | _ | 733 | 731 | 100 | |
| 10 | 0.63 | _ | 749 | 731 | 100 | |
| 12 | 0.66 | - | 736 | 731 | 100 | |
| 15 | 0.67 | - | 670 | 731 | 100 | |
| | npirical Val | | | | | |

used in the foundry. Hence, it was thought that perhaps the washed and dried sands of close grain distribution used in the laboratory were not representative of actual foundry conditions. Yet some simple starting point must be taken and systematic ground work laid before the more complicating factors can be considered.

The accuracy of the green strength test is of the order of \pm 0.05 psi. To obtain this accuracy, considerable care must be given to the preparation and handling of the test specimen and the execution of the test. Data given in Tables 3 to 6 would indicate greater accuracy than \pm 0.05 psi. However, each result given is the average of a number of samples from separately prepared batches of similar composition.

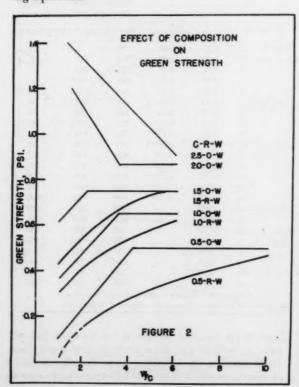
From the data collected, a plot of green strength (GS) as a function of water to cereal ratio (W/C) was found to be the most comprehensive method of representing this property for all conditions (Fig. 3). For the purpose of studying the complicated features of green strength, data are divided into logical groups and are replotted as linear functions wherever possible (Fig. 4). Inspection of the curves in Fig. 3 indicate the proper divisions of green strength study to be:

A. Those in which cereal concentration is below 2 per cent.

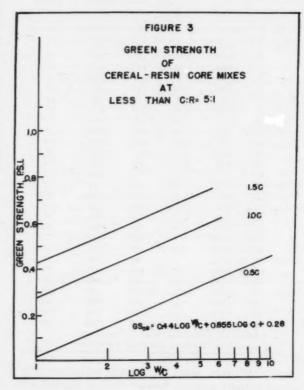
- 1. Cereal alone in the core mix
 - a. Below optimum moisture
 - b. Above optimum moisture
 - c. At optimum moisture
- 2. Cereal and resin in the core mix
 - a. At cereal to resin ratio (C/R) of 5/1 or higher
 - b. At C/R ratio lower than 5/1

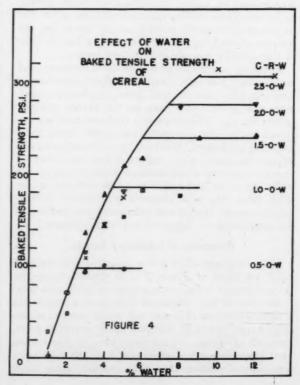
B. Core mixes in which the cereal concentration is 2 per cent or higher.

In core mixes containing less than 2 per cent cereal the green strength may be calculated from the following equations:



- 1. Green strength due to cereal alone in the core mix (GS_e)
- a. Below optimum moisture: GS $_{e} \equiv 0.12~W/C + 0.5C 0.25 \ldots$ Eq. 1





b. Above optimum moisture: $GS_c=0.52\ Log\ C+0.65.\dots$ Eq. 2 c. To determine optimum moisture set

Eq. (1) \equiv Eq. (2) and solve for W.

2. Green strength is reduced when resin is introduced into core mixes containing cereal.

a. When resin is present in only small quantities (C/R =5/1 or greater) the green strength is only slightly reduced. The equations (1) and (2) will approximate the trend expected from these compositions.

b. When resin is present in the core mix in greater proportion than represented by a C/R ratio of 5/1 the nature of the green strength curve shown in Fig. 3 is no longer a linear function of W/C. However replotting on semi-log paper (Fig. 4), green strength when resin is present (GS_{CR}) is found at fixed cereal concentration to be a linear function of log W/C. The GS_{CR} intercepts obtained (i.e. GS_{CR} at log W/C = O) for each cereal concentration were then replotted against various likely functions of C. It was found that these intercept values were a function of log C. Accordingly, the generalized equation becomes:

$$GS_{CR} = \log W/C + 0.85 \log C + 0.28....Eq. 3$$

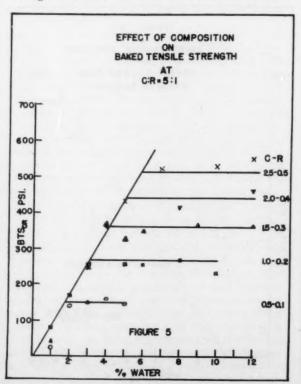
The development of green strength in core mixes containing cereal as the only binder is a function of the water to cereal ratio. Green strength increases as both the water and cereal increase. At a given cereal content green strength will increase with increasing water content up to a maximum at a certain critical water level. This critical water requirement will lie between 2.0 and 3.5 per cent depending on the cereal concentration in the core mix.

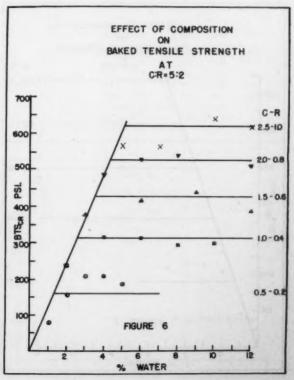
When resin is introduced into the core mix, green strength still remains a function of the water to cereal ratio. However, the amount of green strength produced is less than that expected from the same quantity of cereal alone in the core mix. Cereal-resin core mixes do not exhibit the characteristic critical water requirements of cereal alone. From the curves given in Fig. 3 it would appear that increasing the W/C ratio for cereal-resin core mixes GS_{CR} would increase indefinitely. In practice, it is possible to approach the green strength expected from cereal alone by increasing the water to cereal ratio of the resin core mix. But it must be understood that within the tacit limits of the equaions given and practicable handling of the core mix in the foundry indiscriminate increase of the water to cereal ratio will never give green strengths exceeding that obtainable from cereal alone.

Between 1.5 and 2.0 per cent cereal alone in a core mix, a radical change in green strength characteristics of cereal is observed. When progressing from 1.5 to 2.0 per cent cereal in core mixes at low water to cereal ratios, very large increase in green strength is observed. Accompanying this increased strength the core mix exhibits a very plastic, sticky consistency. Since precise words are not available to describe this condition, it must be warned that this condition is not the same as that normally described as a "tough" core mix in the foundry.

These mixes decrease in green strength as the water to cereal ratio increases. When resin is introduced into these systems, the strengths drop rapidly to about that obtained from 1.5 per cent cereal alone and no change in strength is observed at various water to cereal ratios. No attempts have been made to analyze these conditions except to note their presence since seldom will they be encountered in the foundry.

The specific effects of various additives normally





employed in foundry core mixes have not been thoroughly evaluated. However, the following qualitative characteristics may be ascribed to various materials as they affect green strength:

| Additive | Effect on Green Strength |
|------------------|--------------------------|
| Natural Clay | Increase moderately |
| Bentonite | Increase considerably |
| Kerosene | Decrease slightly |
| Dextrine | None |
| Iron Oxide | None |
| Silica Flour | Increase slightly |
| Drying Oils | Decrease slightly |

Cereal will vary depending on the source and processing techniques. It is not expected that the general characteristics of green strength formation will change with various cereals but only the degree of green strength produced will be changed.

Baked Tensile Strength

Baked tensile strength values obtained from various core compositions presented in Tables 3 to 6 were plotted as shown in Fig. 4 to 7. The reasons for organizing and plotting these data in the manner shown will be discussed later in this report. Replotting the data on semi-log paper with properly selected coordinates (Fig. 8 to 11), linear configurations resulted from which the following equations were resolved:

Baked Tensile Strength of Cereal:

a. At less than optimum moisture

$$BTS_{c} = \frac{Log W - 0.218}{0.0025} \dots Eq. 4$$

b. At optimum moisture
BTS_C = 302 Log C + 186Eq. 5

c. To determine optimum moisture set

Eq. 4 = Eq. 5 and solve for W Baked Tensile Strength of Cereal-Resin Core Mixes:

a. At less than optimum moisture

BTS_{CR} = W (172-122 Log C/R) Eq. 6

b. At optimum moisture $BTS_{CR} = (302 \text{ Log C} + 186) + (220 \text{ R} + 186)$

How well these equations describe the observed behavior of given compositions may be determined by comparing the observed and calculated figures given in Tables 3 to 6.

A study of these graphs and equations will bring forth some pertinent observations which are essential to the successful operation of resin in the foundry:

1. A certain amount of water is necessary to develop the maximum strength from a given quantity of cereal.

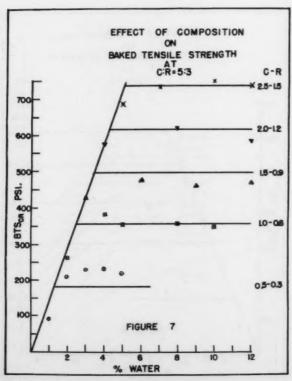
2. As the water content is decreased below this critical level, baked strength decreases rapidly.

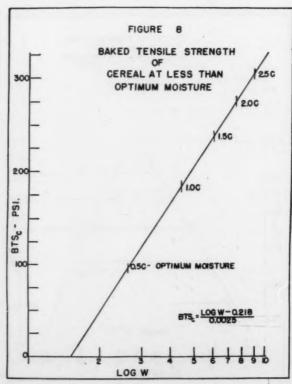
3. No change in baked strength is observed if the water is increased beyond the optimum limit.

4. If cereal alone is used in the core mix, the minimum water requirement will be between 2.5 and 9 per cent for cereal concentrations of 0.5 to 2.5 per cent.

5. Incorporation of resin into the core mix increases tensile strength and requires less water to develop maximum strength than for the same quantity of cereal used as the only binder in the mix.

The baked tensile strength of core mixes is dependent on the concentrations of cereal, resin and water in the mix. Intrinsically, the tensile strength of a core is due to cereal and resin. The water evaporates and

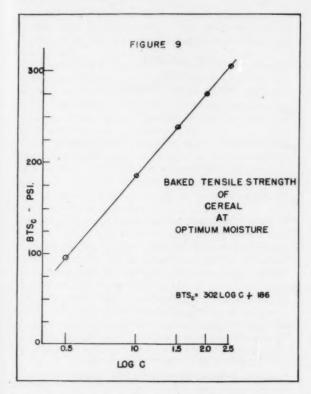




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fu or re pr

ce



does not add materially to the strength of the core. However water is indispensible for the development of cereal and resin strength. The exact function of water is not known but it appears to activate the cereal and disperse the resin. It was assumed that tensile strength was the sum of the tensile strength due to cereal plus that of the resin.

It was found possible to determine the baked strength of cereal directly. It was not possible to determine the strength of the resin bond directly since resin without cereal in a core mix will migrate to the surface of the core during normal baking, resulting in poor cross-sectional bond. Hence, it was necessary to devise a method of procedure by which the resin strength could be calculated indirectly. In initial tests it was thought that both resin efficiency (RE) and cereal efficiency (CE) expressed as psi tensile strength per unit of specific binder could be arrived at by the use of simultaneous equations based on C + R + W = BTS. For example, given the following conditions:

- (1) 0.5C + 0R + 4W = 101 psi
- (2) 1.5C + 0R + 4W = 177 psi
- (3) 1.5C + 0.3R + 4W = 370 psi

by subtracting (2) - (1) and (3) - (2) the results may be expressed as CE = 76 and RE = 643. At this time sufficient data were not available to recognize that simultaneous equations would apply to only a limited number of compositions since the relationships between cereal, resin and water are not simple linear functions. Working with the view of obtaining data on core mixes of such composition that they would readily apply to the simultaneous equation theory a program was outlined. Strengths were determined on cereal concentrations from 0.5 to 2.5 per cent at vari-

ous water levels of 1 to 12 per cent as shown in Fig. 4. Resin was then introduced into a similar system, three cereal to resin ratios (C/R) were selected, cereal-resin composition was varied by unit multiples of the C/R ratio over a 1 to 12 per cent water range as shown in Table 7.

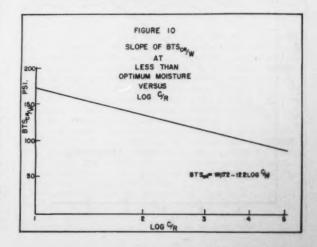
TABLE 7 Multiples C:0 5:1 5:2 .5:3 0.5-0-W 0.5-0.1-W 0.5 - 0.2 - W0.5-0.3-W 2 1.0-0-W 1.0-0.2-W 1.0-0.4-W 1.0-0.6-W 1.5-0.6-W 1.5-0.9-W 3 1.5-0-W 1.5-0.3-W 4 2.0-0-W 2.0 - 0.4 - W2.0-0.8-W 2.0-1.2-W 2.5-0-W 2.5-0.5-W 2.5-1.0-W

When the resulting data were tabulated it was found to be much simpler to plot the data as in Fig. 4 to 7 and resolve equations to fit the curves graphically (Fig. 8 to 11) rather than to be limited by the involved simultaneous equations. However at optimum moisture the expression of cereal and resin efficiency has particular merit for deriving further information. Thus, the total observed baked strength can be broken down into two components, one due to activated cereal and the other due to resin with the assumption that these two components are additive (i.e. that there is no interaction between them). A comparison of cereal and resin efficiencies and optimum moistures for various C/R ratios are shown in Table 8.

As is typical with most adhesive systems binder efficiency decreases as binder content increases. Cereal

TABLE 8—COMPARISON OF CEREAL AND RESIN EFFI-CIENCY AT OPTIMUM MOISTURE

| | | | | C/R | | | | |
|-----|-----|------|------|------------|------------------------------------|-----|-----|-----|
| | C/O | | 5 | /1 | 5 | /2 | 5 | /3 |
| % C | CE* | OM* | RE* | OM · | RE | OM | RE | OM |
| 0.5 | 192 | 2.6 | 410 | 1.7 | 320 | 1.3 | 280 | 1.3 |
| 1.0 | 185 | 4.4 | 425 | 3.1 | 325 | 2.6 | 283 | 2.5 |
| 1.5 | 160 | 6.0 | 400 | 4.2 | 316 | 3.5 | 283 | 3.4 |
| 2.0 | 138 | 7.4 | 410 | 5.1 | 317 | 4.3 | 283 | 4.2 |
| 2.5 | 122 | 8.8 | 418 | 5.9 | 314 | 5.1 | 286 | 5.1 |
| | | RE = | Resi | n Efficien | ency – j cy – psi/ pisture – | R | | |



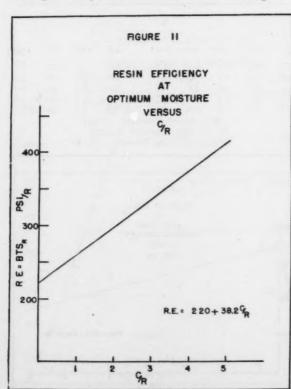
efficiency decreases trom 192 to 122 psi per unit of cereal, over a range of 0.5 to 2.5 per cent cereal. Resin efficiency, while twice as high as cereal, decreases from about 400 to about 280 psi per unit of resin as the resin increases over the C/R range of 5/1 to 5/3. Moisture requirements are drastically reduced when resin is introduced into the cereal system. Cereal alone requires a 2 to 9.0 per cent moisture range to develop maximum strength from 0.5 to 2.5 per cent cereal concentrations. At a C/R ratio of 5/3 this range is reduced to 1.3 to 5.1 per cent.

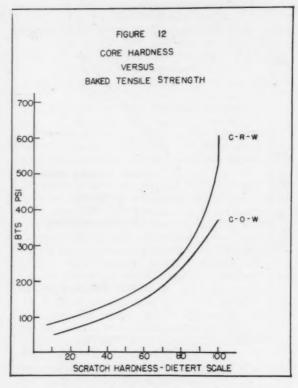
It is obvious that indiscriminate juggling of core composition is not feasible from an economic nor a workability standpoint. It must be realized that while increasing cereal and resin concentration may increase strength a reduction in binder efficiency is also occurring.

It is from these considerations that the composition 1.5-0.3-5 (0.3 per cent resin solids or approximately 0.5 per cent total resin for a resin containing 65 per cent solids) is recommended for use in the foundry.

Evaluation Programs

Data presented for baked tensile strength is thought to be typical for any sand, cereal, resin and water system. Variation of degree but not of type is expected when any of the components are changed. Since all the data necessary to determine the complete range of use for cereal and resin may be condensed into four linear graphs on semi-log paper, it is a simple matter to select the minimum number of core mixes necessary to evaluate any variable of composition. Other variables affecting baked properties of core mixes such as sand grain shape, size and distribution, mulling effi-





ciency, and ramming efficiency may be capable of being evaluated in this manner.

Hardness

Scratch surface hardness is shown in Fig. 12 to be related to baked tensile strength. Increasing baked tensile strength increases the surface hardness of the core. Therefore, those factors that affect baked tensile also affect surface hardness. For example, the addition of resin to a cereal-sand mix not only increases the hardness but also decreases the amount of water required to bring out the maximum hardness. However, in actual foundry practice it may often be desirable to use a water content in the mix below the minimum required for maximum hardness—in order to facilitate blower operations, etc.—and then to spray the surface of the unbaked core with water so as to develop the maximum surface hardness.

Release Agents

%

0.5

1.0

2.5

1.0

1.0-

1.0-

*M

240

Cvc

Add

Mul

Mul

Initial tests were designed to test specifically the effects of cereal, resin and water compositions on core properties. No other additives were used which might influence the measurements desired. Since unlubricated cereal-resin core mixes tend to stick to core boxes, it was necessary to lubricate directly the surface of the core boxes in which test specimens were made. This method was not considered to affect the results obtained.

In practical application it is more convenient to lubricate aluminum and steel core boxes by modifying the mix with 0.25 per cent kerosene and 0.03 per cent parting agent. Sticking in wooden core boxes coated with shellac cannot be eliminated in the same manner as metal boxes because the phenolic resin softens and tackifies the shellac coating. This problem may be eliminated by the use of a vinyl or rubber-based core box coating.

Kerosene has a detrimental effect on the strength properties of cores. Both green and baked strengths are reduced. Parting agents normally used in the lubricant do not affect strength properties but kerosene is a necessary vehicle for the parting agent and cannot be eliminated from the core mix.

The baked tensile strength of mixes containing no resin (only cereal, sand and water) are mostly affected (See Table 9). Strengths are reduced 24 to 29 per cent when 0.25 per cent kerosene is added to these mixes. Increasing the kerosene to 0.5 per cent does not reduce the strength further.

Resin appears to minimize the detrimental effect of kerosene. The baked tensile strength of core mixes containing 0.5 per cent resin solids is negligibly reduced when 0.25 per cent kerosene is added. However the strength is reduced 4 to 14 per cent when kerosene is increased to 0.5 per cent.

Apparently kerosene is affecting the cereal strength of the core. Therefore, the least amount of kerosene necessary to distribute the parting agent should be used. From various laboratory and foundry tests 0.25 per cent kerosene was found to be entirely satisfactory.

TABLE 9-EFFECT OF KEROSENE ON CORE PROPERTIES

| Composition | | een Strei Kerosei | 0 | | ensile Str Keroser | |
|---------------|-----|----------------------|-----|-----|-----------------------|-----|
| %C-%R-%W | 0 | 0.25 | 0.5 | 0 | 0.25 | 0.5 |
| 0.5 - 0 - 5 | .47 | nectority. | .50 | 97 | 69 | 66 |
| 0.5 - 0.1 - 5 | .50 | .37 | .34 | 144 | 140 | 129 |
| 0.5 - 0.5 - 5 | .35 | .35 | .30 | 266 | 273 | 230 |
| 1.0 - 0 - 5 | .63 | .55 | .51 | 162 | 117 | 112 |
| 1.0 - 0.1 - 5 | .51 | .31 | .43 | 200 | 171 | 173 |
| 1.0 - 0.5 - 5 | .52 | .40 | .40 | 355 | 374 | 354 |
| 1.5 - 0 - 5 | .69 | .65 | .73 | 208 | 136 | 133 |
| 1.5 - 0.1 - 5 | .71 | .63 | .58 | 247 | 188 | 187 |
| 1.5 - 0.5 - 5 | .45 | .50 | - | 421 | 423 | 397 |

TABLE 10-COMPARISON OF LABORATORY AND PRODUCTION MULLERS

| Composition | Green | psi | ngth, | Bake | ed Ten psi | sile, | | cratc ardn | | | |
|------------------------------|--------|------|-----------------|-----------------|---------------|--------------|---------|---------------|-----|--|--|
| %C-R-W | A | В | C | A | В | C | A | В | C | | |
| 0.5-0 - 4 | .59 | .62 | .50 | 94 | 124 | 96 | 45 | 50 | 45 | | |
| 1.0-0 - 6 | .81 | - | .65 | 150 | | 186 | _ | - | 63 | | |
| 1.5-0 - 8 | .90 | | - | 175 | _ | 230 | 80 | _ | 72 | | |
| 2.0-0 -12 | .95 | 1.04 | .83 | 195 | 294 | 276 | 91 | 79 | 93 | | |
| 2.5-0 -14 | 1.09 | _ | .97 | 230 | _ | 306 | 100 | _ | 100 | | |
| 1.0-0.2- 6 | .62 | .62 | .62 | 290 | 220 | 268 | 90 | 80 | 78 | | |
| 1.0-0.3- 6 | .56 | .62 | .62 | 317 | 259 | 290 | 95 | 88 | _ | | |
| 1.0-0.4- 6 | .57 | .57 | .62 | 323 | 295 | 312 97 64 95 | | | | | |
| .0-0.5-6 .50 .60 .59 331 319 | | | | | | 334 97 91 — | | | | | |
| *Muller A | | Mı | iller 1 | В | | Mul | ler C | | | | |
| 240-lb batch | | 250 | lb ba | atch | | 25-11 | b batc | h | | | |
| Cycle | | Cy | cle | | | Cycl | e | | | | |
| Add sand and | cereal | | d sand Water | d-1/2 -Cerea | 1 | Sam | e as A | | | | |
| Mull 2 min | | Mu | 11 0.2 | 5 min | | Valu | ies giv | en f | or | | |
| Add resin and | water | Ad | d 1/2 1 | water | | G. | S. and | BT | S | | |
| Mull 4 min | | Ad | d residul 0.5 | | | ar | e calcı | ılate | d | | |

Pilot Plant Evaluations

After evaluation in the laboratory standard compositions (Table 10) were evaluated in two commercial mullers. The laboratory batches were increased from 25 lb to 250 lb in these tests. Approximately 73 per cent of the baked tensile strength data observed were within the \pm 10 per cent limits of the value calculated for each composition. Green strengths agreed well and surface hardness were fairly consistent.

Only one batch of each composition was run in each commercial muller yet the agreement of observed results with those expected is sufficiently good that it is safe to say that correlation between laboratory and foundry techniques can be readily achieved.

Summary

Reproducible and accurate physical property data have been obtained from core mixes and these physical properties have been correlated to core composition. Moreover, the results obtained in the laboratory can be reproduced in the foundry. Considerable work is yet to be done on the correlation of physical properties and core composition to casting quality.

It is hoped that this paper will stimulate interest in the scientific approach to core problems. Criticism of the methods used would be appreciated by the authors.

Acknowledgment

We wish to acknowledge the former Interlake Chemical Corp. in whose laboratories a great portion of this work was accomplished.

Acknowledgment is due to the Beardsley and Piper Division of Pettibone Mulliken Corp., Chicago, and the National Engineering Co., Chicago, for assistance and use of their mulling equipment.

It is a pleasure to acknowledge the services of Chester W. Fitko, formerly of Interlake Chemical Corp., in the design and execution of this program.

DISCUSSION

Chairman: H. K. Salzberg, The Borden Co., Bainbridge, N. Y. Co-Chairman: E. E. Woodliff, Foundry Sand Service Engineering Co., Detroit.

R. B. MELMOTH (Written Discussion): 1 Messrs. McMillan and Wickett are to be congratulated on a sound and well organized investigation which is of immediate value to the foundry industry. Its significance will no doubt be enhanced when the cereal-resin-water relationships are expanded to include the other common additives and their interrelated effects on core properties.

Inasmuch as water plays such a critical role in the development of green and baked strength, it seems logical that materials which influence the distribution of water by surface characteristics, such as adsorption, will have an appreciable bearing on the ultimate properties of the core. Some evidence has been presented to indicate that even in this simplified base system of cereal, resin, and water, some such interaction exists. As a matter of interest, I would like to mention a peculiar condition which might be explained on the basis of adsorption.

We have found after a considerable amount of cross-checking of moisture testing equipment that the calcium carbide moisture teller gives consistently low results on sands containing resin, cereal, water, and kerosene-dispersed release agents. The discrepancy amounts to 1 to 1.2 per cent at a moisture level of 4½ per cent, and is quite reproducible.

The revelation that the presence of resin in a sand mix lowers the optimum water content at any given cerea! concen-

¹ Manufacturing Research, Ford Motor Co., Dearborn, Mich.

tration is most encouraging as anything that enables us to operate at lower moisture levels is doubly advantageous; it improves handling characteristics and reduces baking time. It is sincerely hoped that as an outgrowth of this line of thought resins will be developed which will minimize the use of cereal and attain their maximum properties at very low moisture levels. The industry is awaiting these resins—with open arms.

CHAIRMAN SALZBERG: The authors are to be congratulated on presentation of a most interesting paper full of useful information which can be put to practical applications. Among the things that appeal to me is the fact that the work is keyed into the A.F.S. tests. You will note that the tables and graphs are all in terms of values, figures and tests that we all understand, thereby adding considerably to the value of the data.

Another thing that is somewhat unusual for a paper on binders and a valuable thing to those working in sand is that there is considerable stress on cereal. It is more than an implication in this paper that one must consider the combination of materials that go into the sand. We cannot deal with one material alone, put it into the sand and test its strength and other properties and then assume that with another material doing the same thing we have additive properties. I think that is ably brought out in the data.

The importance of green strength is stressed. Much of the early work on sand binders has been done with stress on dry strength only and the overall scope of core work was not understood. In other words, we not only bind the sand into a core, but we make a precise core, and there is where the green strength applies.

Finally the authors provide equations whereby green strength and dry strength can be calculated. That should be most useful. Further data will make the use of these equations practical.

MR. McMILLAN: We merely evolved equations to fit curves as shown for a standard system. In other words, we used a single sand, single resin, single cereal, at the water concentrations given. Yet it leads to the assumption that if you can do it for a standard system, the system used in any foundry may be made standard. Most foundries change the sand or they change the cereal, or they introduce bentonite, silica flour, iron oxide, or other variables which will have an effect. That effect can be calculated if enough care is given to the investigation of that particular system. To apply the equations to your particular foundry is impossible because your system will be entirely different. Yet the general idea behind the calculation should be the same.

H. Atkinson: ² We have just completed approximately 3,000 tons of phenol-formaldehyde cores. In our core room the conveyor is about 100 ft parallel to the forge shop. We used sands with an oil mix with 3 to 4 per cent clay content for the cores to withstand the vibration from the forge shop. We resorted to using a mix with phenol-formaldehyde and it eliminated the problem. We used 1 per cent natural clay, 1 per cent silica flour and no bentonite.

MR. McMillan: I am glad to hear that. Usually such introduction will reduce the green strength. Normally we depend upon cereal rather than clay in a core mix for green strength.

We avoid using bentonite in a normal resin condition because that absorbs the characteristics. That is not only the case with resin, but with many other liquid binders that might be used. We find that a low concentration of resin normally used in a core mix around 0.5 to 1 per cent will be absorbed by clays and we end up with no baked properties at all. The introduction of the use of natural clays in the sand does not seem to cause this difficulty.

Mr. Atkinson: Clay in excess of 2 per cent in the sand will retard the baking and nullify the effects of the resin.

Mr. McMillan: I am amazed that you can tolerate that much clay in the mix and still come up with good baked properties. Do you find good hardness and good strength with that amount of clay in the mix?

MR. ATKINSON: Yes, many people have been amazed.

MEMBER: He may be getting the advantage of an air set. Is there an appreciable time lag between the making of those cores and getting them into the oven. The air set would be the same as greatly enhanced green strength which would resist the vibration from your forge shop.

Mr. ATKINSON: The plates of cores move along at the rate of five a minute on a conveyor and the workmen pick them up

and put them through the furnace immediately so the cores have no chance to air set.

CO-CHAIRMAN WOODLIFF: What is the moisture content?

MR. ATKINSON: Our sand contains approximately 5 per cent moisture. At times it runs as high as 8 per cent.

E. L. GAELER: ^a You did not mention the type of cereal binder you used in the test. Is there no appreciable difference in the

type of cereal used?

MR. McMillan: Yes, there is a difference due to the type of cereal used in the mix. For the purpose of running these tests we simplified our core mix to those basic materials. We did not vary them while running these tests. We have since then varied them. The type of cereal used will make a change in the result

you get. We have not completed that work yet. As you increase

the solubles in the mix you increase the baked strength that you

can obtain from that type of core mix.

Mr. Atkinson: Did you use corn cereal?

MR. McMILLAN: Yes, we did.

CO-CHAIRMAN WOODLIFF: There certainly is a vast difference in the type of cereals but there also is a difference in the amount of green strength that will be taken away from the mixture as the phenol resins change in age. Whether that is due to instability of resin or whether it is just due to the absorption would be hard to say. In Fig. 2 the lines were drawn for the moisture and cereal sand combination; lines were also drawn after the addition of the resin. The figure showed a normal moisture of 3 per cent, cereal 1 per cent; the strength was about 0.62 psi. But after the resin was added the strength dropped to about 0.4 psi which is a tremendous drop. I believe the producers of this new resin will have to give the foundrymen more green strength before we can say that the material is entirely satisfactory. That is taking away 33 per cent of the green strength by adding material. We cannot afford to put in additional cereal to compensate for this loss. How do the authors compensate for this drop in green strength. They say the use of clay is not good. Use of cereal at 5 or 6 cents per pound is an economic disadvantage.

MR. McMILLAN: I would like to see comparative data on how much core oil reduces green strength on a similar basis before I say that the drop is too much. It was mentioned that particular types of resin vary the green strength. That is true; water soluble resin decreases green strength. There is a hypothetical answer to that. If the reaction of phenol-formaldehyde were permitted to proceed, three phases would result, the water soluble phase, water insoluble phase and the solid fusible stage. If you would compare those three types of resins you would find that once we eliminate the water solubility of resin the green strength will bound right back to that which you expect from the cereal alone. That brings up the question shall we go directly to a water insoluble resin in order not to destroy green strength in the core mix? Baked strength loss with this type resin precludes its use. At the moment we think a water soluble liquid resin is the desirable one. To try to answer further, we might mention also that the introduction of any weting agent into a cereal water mixture will reduce the green strength in that

In normal practice in the foundry, you will notice in the curves presented that as we increase the moisture content in the core mix, we approached the total green strength that we would expect from cereal, we do not quite make it. But the total drop is insignificant compared to what Mr. Woodliff mentioned in that particular instance. Normal operation in the foundry will run approximately $2\frac{1}{2}$ to $3\frac{1}{2}$ per cent moisture. In the use of resins we ask that the moisture be brought up a little. That is one of the reasons why we ask that moisture contents be maintained above 4 per cent. Also, to get the most out of the cereal binder as well as the most out of the resin binder with regard to strength, moisture should be high.

Handleability is another question. That will also vary with regard to sand size.

CO-CHAIRMAN WOODLIFF: To compensate for the loss in green strength necessitated going to a slightly finer sand in order to bring back the green strength to where it was with the oil-sand mixture formerly used. By so doing the use of phenol resins, or synthetic resins, because that includes urea and phenol, have worked very successfully. This ties in with my experience of the slightly finer sand. This will work successfully and will give you green strength.

² Methods Engineer, International Harvester Co., Hamilton, Ontario, Canada

⁸ Foundry Superintendent, Frick Co., Inc., Blue Ridge Summit, Pa.

L. B. Osborn: 4 We find that some sands have considerably more green strength than others, partly depending on whether they have a natural grain distribution or have been screened mechanically from a coarser basic sand. They may be said to have intangible cohesiveness or stand-up strength not dependent on any clay present—or if any natural clay is present, it may be insignificant in amount.

Such sands have been found to develop good green and baked core properties with much less than customary cereal binder content. One sand, with a rather subangular grain shape, develops satisfactory oil-bonded cores with about half the normal cereal content; and with synthetic resin binders gave better performance than lake sand of about the same average fineness. Thus, some consideration of the geologic source of sands, and their conditioning for the market is significant to foundrymen. A study of mixing time, and materials used, will pay dividends to

4 Sales Manager, Tri-State Sand Company, Corinth, Miss.

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foundrymen who are willing to spend some time in search of best results.

MR. McMillan: Could you use less cereal and get the same properties?

MR. OSBORN: Yes.

MR. McMillan: Yes, in this sense we have not found that grain size or grain distribution in the normal range from 60 to 120 fineness has appreciable effect on the baked properties of a core. However, if we go from round grain sand to angular sand or subangular sand, we do have a difference in the main properties. So if in this case you quote a finer sand is used, greater green strength may be expected which would permit a reduction of cereal content. It is possible to get exactly the same baked strength if you go through some of these manipulations shown in the paper. For instance, increasing the resin to cereal ratio you would find a slight increase in strength. You would have approximately the same baked strength if the cereal decrease was not too appreciable.

SELECTION AND APPLICATION OF CLEANING ROOM EQUIPMENT

By

Stanley Krzeszewski*

Many factors are involved in the selection of proper cleaning room equipment. The principal considerations are: (1) cleaning ability; (2) initial cost of the equipment; (3) operating and maintenance cost; (4) safety and hygiene; (5) type of finish produced; and (6) miscellaneous factors such as the floor space required, noise, working conditions, etc.

The relative importance of these factors will vary trom plant to plant and from time to time. Cost is always an important point and usually is the first consideration in the selection of equipment. Because of the over-all importance of the cost factor it will receive special attention throughout this paper.

In cases where production requirements are small, it may be desirable to sacrifice slightly on operating costs in order to reduce the initial investment. In other cases, the need for a special finish may require the purchase of equipment which is initially more expensive as well as more expensive to operate. In still other cases, safety and hygiene requirements may necessitate small sacrifices both in cost and finish.

Advantages and Limitations Listed

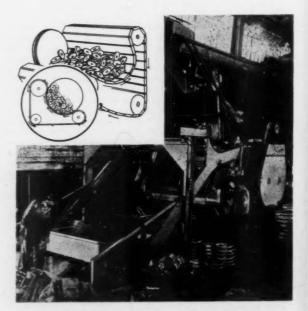
These are decisions which an outsider cannot presume to make for the individual plant manager. Each knows his own conditions best and must carefully weigh the various factors according to the importance his own judgment places on them. The purpose of the present paper is to list and illustrate the various advantages and limitations of the different types of equipment available with the thought of providing foundrymen with basic facts. Since most advantages and disadvantages are relative values, clarity demands that we select some one method as the standard or norm to which all others will be compared. In this paper airless blast cleaning has been selected as the standard.

Airless Blast Cleaning: By utilizing centrifugal force to hurl the shot or grit, airless blast equipment provides an efficient method for throwing tremendous quantities of abrasive. Consequently, it offers high production rates with low labor requirements. In order to handle the volume of work that the process is capable of cleaning, and properly present all surfaces to the abrasive blast, special work-handling systems are frequently necessary. These generally result in an

initial investment somewhat higher than that required for many other cleaning methods.

The process gives a thoroughly cleaned surface which is ideally suited for any subsequent coating or finishing operation. The fine matte finish produced presents an excellent bonding surface for plating or painting operations, and further cleaning or pickling is not generally required. The degree of cleanliness provided depends entirely on the requirements of the individual operator, and may vary from simply knocking off loose sand to the thorough removal of all burned-in sand and scale to leave a chemically clean surface.

With the work being handled mechanically while in the blast chamber, it is not necessary for an operator to work in the blast zone and the equipment can be



Heavy steel castings are cleaned in a typical heavy duty airless blast tumbling machine equipped with a skip for fast loading. The sketch shows the principle of operation of the continuous belt conveyor. As the apron conveyor moves in one direction, the work is tumbled and cascaded directly under the abrasive blast.

American Wheelabrator & Equipment Corp., Mishawaka, Ind.

totally enclosed in dust-tight housings. This, coupled with adequate ventilation, solves the dust problem and provides good working conditions.

Because of the highly abrasive nature of most of the non-metallic grits such as sand, aluminum oxide or silicon carbide, they cause excessive wear on airless blast equipment and their use is generally discouraged. An exception to this is in the use of the so-called "soft grits." These include such materials as crushed apricot pits or crushed walnut shells, which have but limited

application in the foundry field.

Tumbling Equipment: One of the oldest and most widely used cleaning methods, the "tumbler" or "rattler," still is used by many foundries, especially the smaller ones. Tumblers have the advantage of a lower initial investment and of producing a type of finish which is very desirable in some applications. The rounded corners, partial removal of flash and smooth surface provided by the tumbler gives a pleasing appearance on some types of parts, and the method is occasionally written into specifications.

On the other hand, tumbling has several disadvantages. It has a relatively high operating cost from the standpoint of both labor and power required. It does a poor job of actually cleaning the surface of the casting, which is particularly undesirable if the casting is to be machined or plated. It leaves a film of dust and burned-in sand on the surface that resists plating and

damages tools.

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Make Cost Comparisons

Tumbling equipment requires considerable space for a given amount of production and is extremely noisy when steel liners are used in the barrel. Since the higher operating and maintenance costs of tumbling equipment nullify its lower initial cost, the only advantage to be claimed is the "tumbled finish" where this is desirable.

Here is a cost comparison between tumbling and airless blasting in a gray iron foundry producing agricultural castings. Five tumbling mills were replaced by one airless blast tumbling machine. Production increased from 10 tons cleaned in 9 hr to 20 tons cleaned in 10 hr. In spite of the doubled production, labor requirements decreased from 27 to 20 man-hours. Cleaning cost was reduced from \$4.19 to \$2.09 per ton.

Another example involves the cleaning of building hardware in a small foundry. Three tumbling mills were replaced by one airless table-type machine. Although production increased from 2 tons to 4 tons per day, labor requirements were reduced from 12 to 6 man-hours, and cleaning costs from \$8.09 to \$2.65

per ton.

Sandblast: Cost-wise, sandblasting will frequently offer an advantage in the form of lower initial investment when an adequate supply of compressed air is already available. In the case of sandblast rooms requiring a large pit, or in those cases where additional air compressor capacity is required, this advantage may be lost.

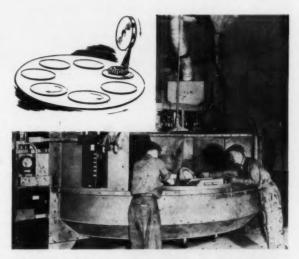
The two inherent advantages of sandblast equipment are its ability to clean interior surfaces and the fact that non-metallic abrasive can be used economically. The narrow stream of abrasive projected through the conventional nozzle is ideally adapted for cleaning

the interior surfaces of pipe or deep, narrow recesses in castings. In fact, an airblast touch-up chamber is sometimes incorporated in airless blast machines for

this purpose.

Cast iron and steel abrasives may occasionally give unsatisfactory results on some types of materials such as stainless steel, aluminum, magnesium, and a few others. In these cases it is frequently recommended that a non-metallic abrasive be used. Since most of the non-metallic abrasives such as sand, silicon carbide or aluminum oxide cause excessive wear on airless blast equipment, they are generally used in conventional airblast types of equipment.

The process has several disadvantages, the principal one being the high operating cost of the equipment.



The sketch illustrates the operating principle of the multitable-type machine. The individual work tables rotate as they pass through the abrasive blast, exposing all casting surfaces to the cleaning action of the blast. In the operation shown, one man is loading and the other unloading the tables as they pass the opening.

A considerable amount of manual labor is generally required in the operation of sandblast equipment and the production rate is low, resulting in a high labor cost per ton of castings cleaned. Another important expense factor is the cost of compressed air. Where sizable production is involved, these high operating costs more than offset any saving in initial investment. Considerable difficulty is encountered in keeping the air dry and special traps and bleeds must be provided.

Unpleasant working conditions are generally found in sandblast operations because of the clouds of dust created, especially in sandblast rooms where the operator must work in the dusty atmosphere. Various precautionary measures such as the use of helmets, protective clothing and adequate ventilation tend to

alleviate this condition.

Where uniformity of appearance is important over a large area, considerable difficulty may be experienced if airblast equipment is used. The narrow concentrated blast from the nozzle produces a mottled or a striped appearance unless unusual care is exercised. In a large steel foundry pouring 1300 tons per month, three airblast rooms were replaced by a large airless blast tumbling machine. Labor requirements were cut from 50 to 19 man-hours daily, and cleaning costs from \$3.56 to \$2.41 per ton. In another steel jobbing foundry an airblast room and table were replaced by a special airless table-type room. Labor requirements were reduced from 50 to 24 man-hours daily, and cleaning costs from \$4.75 to \$3.38 per ton.

Wet Blasting: This method may be broken down into two general classifications—(1) core knock-out and/or heavy cleaning where high-pressure water, with or without the addition of sand, is the cleaning agent; and (2) fine finishing operations where water and finely divided abrasive materials are propelled by com-

pressed air.

Both classifications have the great advantage of practically eliminating dust if the castings are cold. It has the same disadvantages as sandblasting except for the dust hazard. Because of the cost of high-pressure water, slow cleaning rate and manual labor required, the operating costs are generally high and may be prohibitive if any appreciable production volume is to be maintained. For fine finishing operations,

liquid blasting has the advantage of permitting the use of extremely fine abrasives (even down to an estimated 2500 mesh) which could not be successfully handled in the dry state. For certain types of finishes this is a fundamental advantage.

In his book, Modern Blast Cleaning and Ventilation, C. A. Reams gives several examples of the cost of hydraulic blasting compared to hand labor in core

knockout. A few of these are:

| Part | Hydro | Hand |
|-------------------|--------|---------|
| Main engine frame | \$0.80 | \$ 6.00 |
| Bed Plate | 1.40 | 14.00 |
| Turbine end | 7.20 | 30.00 |
| Large runner | 6.00 | 140.00 |

Unfortunately, no actual case histories are available to illustrate the relative economy of wet blasting and

airless blasting.

Pickling: While pickling is not as common a foundry operation as some of the other cleaning methods, it is encountered to some extent, especially where further finishing operations are required. Pickling offers the advantage of relatively low-cost operation plus the production of a thoroughly cleaned surface which is well suited for subsequent plating or coating.

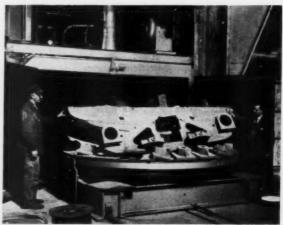
The disadvantages of pickling are well known in the

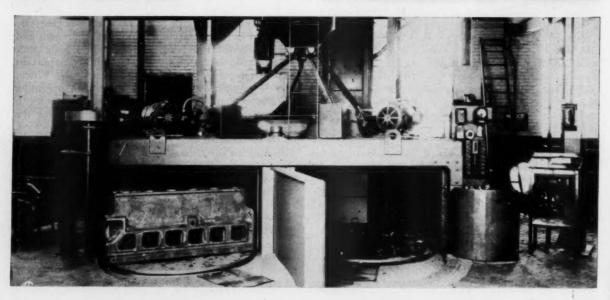


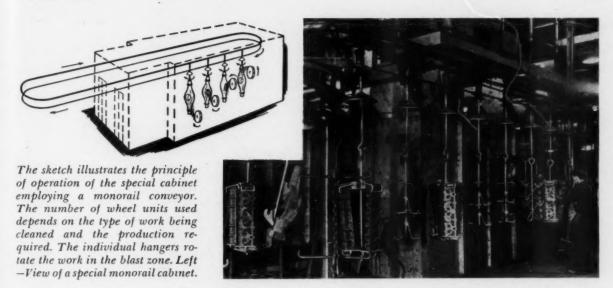
A swinging table typeairless blast machine.

A special cabinet has a rotating table mounted on a traveling car. This type of cabinet is particularly applicable for cleaning very heavy castings.

Diesel engine castings are cleaned on this twin table type machine. While one table, loaded with work to be cleaned, rotates in the blast zone, the other is in the open for loading or unloading.







metal working industry. The most common objections are: (1) defects due to hydrogen embrittlement and smut formation; (2) the loss of virgin metal; (3) its relative ineffectiveness on some acid-resisting materials; (4) the hazard and unpleasantness of working with strong inorganic acids; and (5) the problem of disposal of the waste pickle liquor. In view of recent legislative efforts to reduce stream pollution, the problem of acid disposal is becoming increasingly serious.

Cleaning Costs Reduced

Recent developments in abrasive material have now reduced the cost of airless blasting to a point where it is competitive with pickling. In some cases it is actually less expensive.

Another case study, involving a forge shop instead of a foundry, offers a composite picture on several types of equipment. One medium size airless blast tumbling machine replaced five pickling tanks, two tumbling barrels, and one airblast table in cleaning 1000 tons per month. Labor requirements were reduced from 64 to 16 man-hours daily, and cleaning costs came down from \$3.19 to \$1.64 per ton.

Equ'pment Design: The design of equipment for handling the work in the cleaning room varies widely, depending upon the type and size of the parts to be cleaned and the cleaning method used. As pointed out earlier, work-handling methods become especially important in airless blasting. In many cases essentially the same handling methods may be used for other forms of cleaning, although tumbling, sandblast and pickling equipment generally rely on relatively simple systems involving manual operation.

The barrel for tumbling work is a basic type of handling method that is utilized by practically all of the cleaning methods. It is ideally suited for rugged, compact pieces which can withstand tumbling action.

In simple tumbling mills the barrel is partially filled with the work to be cleaned and rotated until most of the sand and scale have been removed by the action of the work pieces on each other. Jack stars may be added to the charge to assist in digging the sand and scale out of small pockets and recesses. In variations

of this process certain liquids may be added to assist in the cleaning action or to serve as a carrier for abrasive compounds. In addition, the liquid may serve to reduce the creation of dust during the operation.

In airblast equipment it is obviously necessary that some sort of opening be provided through which the abrasive stream can be projected. This requirement was met by a type of equipment known as a tilted barrel—a machine basically similar to the earlier tumbling mill except that an opening was provided in one end, and that end was tilted up from the horizontal.

With the development of airless blasting it was necessary to have the barrel open over a considerable area to accommodate the wide, fan-shaped blast from the wheel unit. This condition was met by the use of a continuous belt conveyor shaped somewhat in the form of the letter J. This provided a constant, uniform tumbling action as in the earlier barrels but left the top of the mill open for mounting the wheel unit.

Handles Fragile Castings

Since the cleaning was performed by the blast stream, a violent tumbling action was not necessary and the continuous belt conveyor made it possible to handle the more fragile pieces. It offered another advantage in the ease with which the work could be inspected and unloaded. Being open on one side, as well as on top, it was necessary only to shut off the abrasive blast and open the door to ascertain the state of cleaning. For unloading, the conveyor could be reversed and the work automatically carried out into waiting tote boxes.

Table-Type Machines: For work which must be handled in considerable quantities but is too fragile or too flat to tumble satisfactorily, table-type machines were developed. Beginning with a simple plain table which revolves about its axis and carries the work through the abrasive blast, numerous variations have been devised. These include the multi-table where individual revolving work tables are passed through the abrasive blast, tables mounted on swinging doors, turntables, tables mounted on traveling cars, and other minor variations. Although most of these have been developed for use with airless-blast equipment, they

can be used in conjunction with airblast if desired.

Each of these table-types have certain advantages and limitations. For simple flat work the plain table generally offers adequate cleaning and production with the minimum initial investment. Where high vertical sides and deep recesses must be cleaned on work that is not suitable for tumbing, and if a relatively high production rate must be maintained, the multi-table is generally used. As the work rotates about its own axis while passing through the blast it offers an opportunity for the abrasive particles to strike it from virtually every angle.

If production rates are not too high and the work is of a large, bulky, or widely varying nature, the swinging table-type machine offers many advantages. With the table mounted on the door, the act of opening the door swings the table out into the open where it can be easily loaded and unloaded by means of overhead hoists. The blast chamber is completely enclosed during blasting operations. Since it will handle practically any shape or size of work up to a certain limit, it has become a handy tool for the jobbing foundry.

High Production Equipment Designed

Its principal disadvantage lies in the fact that it is intermittent in operation because of the necessity of shutting off the abrasive flow when the door is opened. However, this disadvantage can be corrected by means of the twin-table, where one table is being loaded while the other is in the blast chamber. In all table machines at least two passes are generally required to thoroughly clean the work on all sides. Where over-all cleaning coupled with high production is required, a specially designed cabinet may be preferred.

Special Designs: As the name implies, the special cabinets are designed specifically to handle a certain problem, and include a virtually unending list of different handling methods. One of the most common of these

uses a monorail conveyor to carry the work through the blast zone. The work hangers on the conveyor may be rotated as they pass through the blast. In some cases they may stop and index in order to allow additional cleaning time. This type of equipment offers extremely high production rates at low cost and facilitates the cleaning of unusually shaped pieces such as bath tubs and cylinder blocks.

Another continuous-type machine is a variation of the tumbling barrel which permits the work to pass through the machine in a continuous flow. Employing a continuous belt conveyor similar to that described earlier, the work is fed in at one end and is tumbled under the blast stream as it passes, by gravity, through the machine to discharge at the other end. The continuous airless blast tumbling machine permits continuous, automatic operation from shakeout through cleaning, sorting and chipping to shipping room.

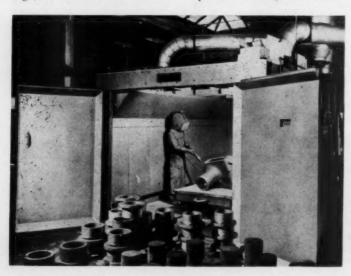
Rooms and Cabinets: This type of equipment is generally found in airblast installations. The room consists of a large covered framework equipped for adequate ventilation, and with a hopper arrangement for removing scale, sand and abrasive and then recovering the usable abrasive. The room is generally used for extremely large work which cannot be conveniently handled in a specially designed cabinet and is not produced in quantities sufficient to justify the cost of specially designed, heavy-duty handling equipment.

Where small numbers of small pieces are to be cleaned by airblast, they are frequently handled in small cabinets where the operator can stand outside the blast chamber and handle the parts manually. His hands and arms extend through apertures in the cabinet wall and are usually protected by rubber gloves.

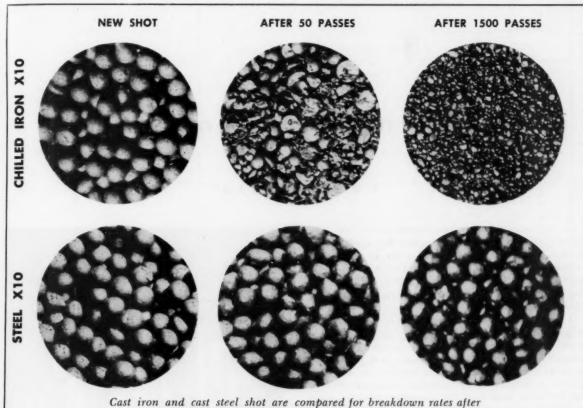
Accessory Equipment: Aside from the basic machine required for handling the work and propelling the abrasive, certain accessory equipment is always re-

A view of a sandblast cabinet showing the operator in working position. Used abrasive falls through a floor grating for collection and re-use.

The operator is preparing to clean a small water meter assembly in an airblast cabinet. Rubber gloves protect operator's hands from the blast.







Cast iron and cast steel shot are compared for breakdown rates after a given number of passes through a centrifugal blasting wheel. X10.

quired. It generally includes a hopper arrangement which gathers up the abrasive, sand and scale and delivers it to a central location from which it is conveyed by means of screw conveyors and elevators to the abrasive separator. This separator is a device employing the principle of air flotation to separate the sand and broken abrasive from the whole, re-usable shot or grit.

In blast cleaning, whether it be airless or airblast, considerable quantities of dust are created from the breakdown of the scale, sand and abrasive. Adequate ventilation and a good dust-collector system are necessary to prevent this dust from becoming a nuisance in the working area.

Abrasive: The list of available abrasives is long, and new ones are constantly being added for specific purposes. In general, the only abrasives that foundrymen are interested in are the non-metallic (sand, silicon carbide, aluminum oxide, etc.) and the metallic (chilled iron, malleableized iron, steel or copper). The metallic abrasives are by far the most popular because their long life and low equipment wear offers much more economical operation. Sand, even when present in small quantities as a result of inadequate separation, has an extremely serious effect on the life of any of the parts in the blast zone.

If full advantage is to be taken of the economies offered by the metal abrasives, care must be exercised to eliminate loss through carryout or leakage from the machine as well as the presence of sand in the circulating abrasive load. This becomes especially important in the case of the premium abrasives such as steel or copper shot. These materials offer greatly reduced wear on the equipment and low shot consumption, even when compared to the other metallic abrasives, but all of the advantages they offer can be nullified by careless operation of the equipment.

In any description of airless blast equipment the importance of sturdy construction and the generous use of heavy, replacable liner plates to protect the cabinet itself from excessive abrasive wear should not be overlooked. Although heavy construction means a somewhat greater initial investment, the maintenance economies it offers far outweigh the first cost.

Even though the cabinet walls are of heavy ½-in. plate, they should not be subjected to any concentrated abrasive blast. Once a hole has been worn through the cabinet wall a whole section must be replaced or unsightly patches tacked on. It is easier and far less expensive to hang ½-in. or ¾-in. liner plates over areas subject to wear.

To summarize briefly the relative advantages of the different cleaning methods, we may say that airless blasting offers thorough cleaning at lowest cost where reasonably large production is involved. In cases of specific jobs, each of the other cleaning methods offers distinct advantages which must be balanced against their inherent disadvantages. This can be done only by the foundryman in the light of his own needs.

SOME TESTS ON RELAXATION OF CAST IRON

By

V. T. Malcolm* and S. Low*

ALTHOUGH RELAXATION PROPERTIES OF CAST IRON are of little interest in most engineering applications of cast iron, the relaxation tests are a convenient means of determining the efficacy of stress relief. The time-residual stress curves also permit each user to choose a stress relief treatment adequate for his particular purposes.

The relaxation test, as reported herein, is a constant strain test, that is, the sum of the elastic and plastic strain is held constant throughout the test. An extensometer is arranged in such a manner as to permit the stress to decrease as the strain attempts to increase. This is accomplished by opening a solenoid valve in the hydraulic load tank. A recording pressure gage, connected to the load tank, continuously records head of water versus time. Since head of water may be directly converted to stress in the bar, an autographic record of residual stress versus time is obtained.

The relaxation test machine, developed by The Chapman Valve Mfg. Co., is shown on Fig. 1. Test specimens were machined from halves of 24-in. x 2-in. x 1-in. transverse bars. The details of the test bar are shown on Fig. 2.

The relaxation properties of both an unalloyed 30,000 psi and an alloyed 50,000 psi cast iron were determined. The chemical compositions of the irons tested are given in Table 1.

TABLE 1

| Specification | ASTM A126-46T Cl. B | ASTM A48-46T Cl. 50 |
|------------------|---------------------|---------------------|
| Total Carbon | 3.34 | 3.30 |
| Graphitic Carbon | 2.64 | 2.60 |
| Manganese | 0.80 | 0.64 |
| Silicon | 1.93 | 1.53 |
| Phosphorus | 0.154 | 0.146 |
| Sulphur | 0.122 | 0.108 |
| Chrcmium | | 0.65 |
| Nickel | | 1.76 |
| Molvbdenum | | 0.61 |

Figure 3 is a typical curve illustrating the decrease

Fig. 1

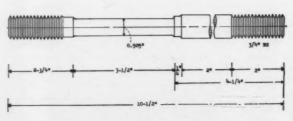


Fig. 2

[•] Director of Research and Research Engineer, The Chapman Valve Mfg. Co., Indian Orchard, Mass.

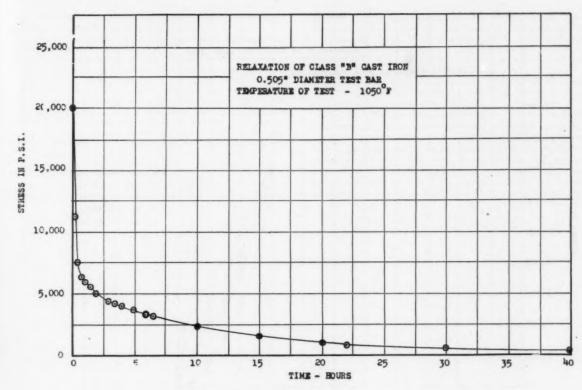


Fig. 3

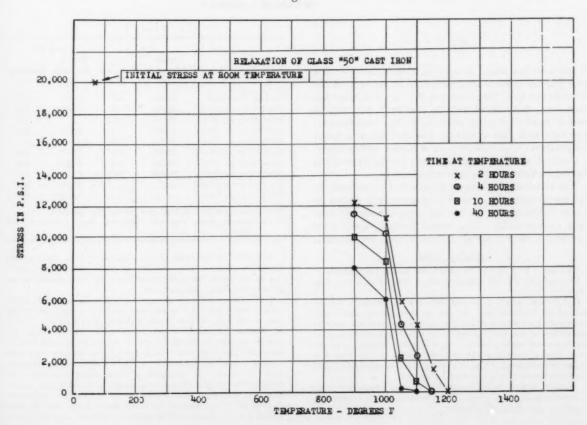


Fig. 4

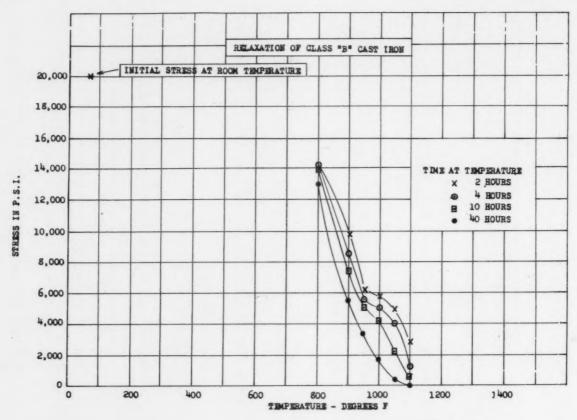


Fig. 5

in stress versus time. It may be noted that the first hour at heat relieves approximately 70 per cent of the stress in the bar. Since the test bar has a section less than 1 in., the rule of holding one hour per inch of section would be quite effective at this temperature. The one hour rule would be less effective at a lower temperature, more effective at a higher temperature.

Figure 4 and Fig. 5 are summary curves illustrating the dual effects of time and temperature.

The effect of the various stress relief treatments on the room temperature tensile properties is shown in Table 2. Obviously, the holding times far exceed those used commercially. The actual holding times were for the complete removal of stress at each temperature.

DISCUSSION

Chairman: F. J. Walls, International Nickel Co., Detroit. Co-Chairman: T. D. PARKER, Climax Molybdenum Co., New York.

E. A. STICHA AND J. J. KANTER (Written Discussion): ¹ Relaxation properties have been utilized for determining stress relief treatments for steels in much the same manner as the authors' application to cast iron. It should be noted that results as given are dependent on the initially applied stress (20,000 psi in this case); higher loading would shift the curves of Fig. 3 to 5 to higher temperatures. Thus, if another starting stress were used, the quantitative relations would be altered.

Volume growth in unalloyed cast iron at moderately high temperatures, it would seem, should be discussed in relationship to tests of the character presented.

¹ Research Laboratories, Crane Co., Chicago.

TABLE 2

| Class 50 Tensile |
|---------------------------------------|
| Tensile |
| ness Strength, Hardness in psi Bhn |
| 5 51,140 255 |
| 3 41,250 235 |
| 7 |
| 0 45,750 255 |
| 9 |
| 7 43,500 235 |
| 3 42,250 212 |
| 3 42,500 217 |
| 44,250 217 |
| 42,750 201 |
| |

* Determined from standard cast tensile bar (as-cast)

 Determined from specimen machined from transverse bar (as-cast)

Additional information on the construction of the machine would be welcome. It is not entirely clear whether this unit is free of the elastic behavior characteristics of some previously described relaxation test machines.

J. H. SCHAUM (Written Discussion): ² The authors have duplicated a small part of a much more extensive project on relaxation of cast iron entitled "Stress Relief of Gray Cast Iron," by J. H. Schaum, published in vol. 56 of A.F.S. Transactions. Although the design of the relaxation testing equipment differed somewhat the tests were conducted in like manner—with one notable exception. According to Fig. 4 and 5, Messis. Malcolm and Low loaded the specimens at room temperature and then heated them to the testing temperature. Thus thermal expansion and relaxation were acting in the direction of expansion during the time required to reach temperature and the load would have to be reduced in order to maintain constant strain. It would make it easier to compare data of other investigators (including myself) who heated specimens to temperature and

² Metallurgist, National Bureau of Standards, Washington, D. C.

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ine t is deplixa-J. Alred one olm nen anion oad ain. tors and then loaded, if the stress values were also shown at the moment the specimen reached the testing temperature.

In spite of this difference in technique the data obtained for the authors' class "B" and class "50" irons agree remarkably well with similar irons used in my work. This in itself is encouraging since previous stress relief studies of cast iron have been notably inconsistent and incomparable.

MR. Low: The reason we had duplicates on Mr. Schaum's work was because we were trying to correlate stress relief treatment with cracking of castings that were overlaid by means of welding. We found excellent correlation where we attempted it. With reference to the data shown in this paper we found it was possible to lay crack-free deposits as well as to eliminate any other trouble.

With respect to Mr. Sticha's comments, the higher the curve, the lower the shift.

There is no particular elasticity for this type of machine. I presume Messrs. Sticha and Kanter were referring to the Barr-

Badgett type of machine. In this type of machine the elastic behavior of the machine has been completely cancelled out. The complete description of the machine used is given in a paper by E. L. Robinson, "High Temperature Bolting," ASTM Proceedings, vol. 48, p. 214 (1948), discussion by Malcolm and Low, p. 236.

MEMBER: In the case of a part which is expected to remain under tension during operation, would there be any advantage in presetting it in relaxation testing?

MR. Low: We have a similar problem in the case of a flanged joint operating at a high temperature where we preset to a higher initial stress. If we take an SAE 4140 stud made up with carbon steel nuts and stress the joint at room temperature to 60,000 psi, when that joint comes to equilibrium after operation at 950 F we will have on the order of 18,000 psi residual stress. We have no experience in trying to preset cast iron but it would be reasonable to presume that it would have some value.

PRECISION CASTING ALUMINUM IN MOIST INVESTMENT MOLDS

By

H. Rosenthal and S. Lipson*

A TECHNIQUE HAS BEEN DEVELOPED for retaining varying amounts of moisture in plaster-bonded investment molds in order to improve the properties of aluminum alloys cast by the precision investment process. The new mold consists of a precoat applied to the wax patterns, over which a backing investment is poured. The backing investment contains a large number of air bubbles to increase its permeability so that the steam generated by the hot metal in the moist mold can escape.

Test bars cast into these molds showed an increase of 15 per cent in tensile strength. A comparison of this technique with the standard investment process indicates a potential saving in investment material, as well as in labor requirements for investing large numbers of molds.

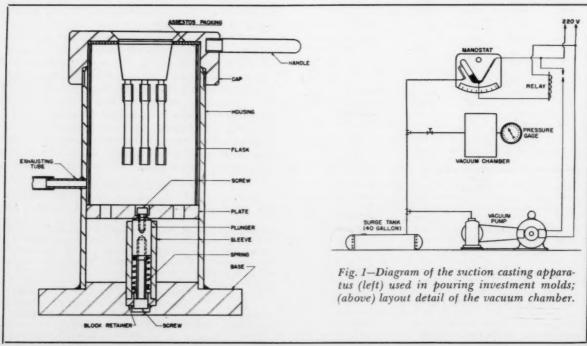
The importance of cooling rate on the mechanical properties of cast aluminum is well known. Standard investment molds are completely dried and of very low heat conductivity. It is to be expected that the mechanical properties of aluminum alloys cast into such molds are low relative to castings made in metal or even green sand molds.

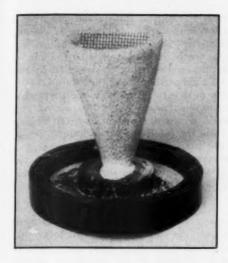
* Metallurgists, Dunn Laboratory, Frankfort Arsenal, Philadelphia. The problem of improving properties of aluminum investment castings resolves itself into one of finding some way to increase the chilling power of the investment. Three techniques were considered for improving the heat conductivity of the investment: (1) adding a conductive material to the standard investment; (2) refrigerating the mold prior to pouring the metal; and (3) retaining moisture in the investment as a cooling medium.

Addition of a conductive material was not considered promising because only a limited amount of such a material can be added without destroying the nature of the investment. In this amount the grains of conductive material would be surrounded by the refractory so that no continuous network of the conductive material could be established. Without such a continuous network the improvement in chilling power would be negligible.

Refrigeration of the mold was considered feasible but did not seem to promise any great improvement so far as properties were concerned. It was felt that although the lower mold temperature would cool the metal faster, fundamentally the rate of heat transfer through the refractory would still be low.

Moisture used as a cooling medium, as suggested by





← Fig. 3—This precoated wax pattern is mounted on a rubber sprue base, and has been reinforced with a screen in the center.

Fig. 4-A plaster mold sectioned to show the permeable investment material surrounding the precoated wax pattern.



the third method, was considered the most promising of the three. It provided the greatest ability to chill the metal since the vaporization of water is a potent means of abstracting heat. In addition, the mechanical problem of adding water to the investment, or retaining the proper amount, appeared relatively simple.

Eliminating Wax

Before the retention of moisture could be considered it was necessary to have a method of eliminating the wax at a temperature compatible with the retention of the required amount of moisture in the investment. The standard technique¹ is to melt out as much wax as possible in a low-temperature oven and then burn out the remainder at high temperature.

When the wax is melted out in an ordinary oven an accompanying loss of moisture from the investment occurs. The melting wax thus has an opportunity to seep into the capillaries formed by the escape of the moisture. Once the wax has been absorbed into the cellular structure of the partially dried investment, it cannot be climinated in a reasonable time at temperatures under 800F (425C).

Previous work had shown that substantially complete wax removal could be made in a steam autoclave. In this saturated steam atmosphere an equilibrium is set up between the moisture in the atmosphere and the moisture in the mold, so that in spite of the elevated temperature there is no tendency for mold dehydration. The molten wax can thus be drained with no appreciable residue requiring elimination at

high temperature. This autoclave treatment was used to remove the wax from all of the molds tested.

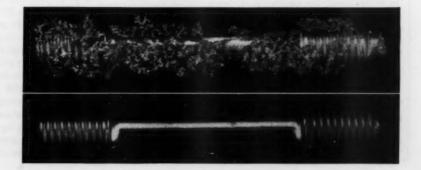
As a logical first experiment, the standard 70 per cent silica—30 per cent plaster investment was used. This investment is designed for use in the completely dried condition. The mechanically held water can be eliminated by baking the mold at a temperature of 212F (100C), after which the chemically held water in the gypsum is driven off by heating at a higher temperature. The usual method for elimination of the wax automatically requires a much higher temperature than is needed for the dehydration of the gypsum. However, having discovered the possibility of eliminating the wax by the autoclave treatment, it was feasible to retain as much moisture in the mold as desired.

Permeability of the investment material is a function of the voids present. These voids are formed when the mechanically held water is driven off. Therefore, it was decided to use a mold which had been heated sufficiently to eliminate the mechanically held water but which still retained the chemically bound water.

The prepared mold was placed in a suction casting device (Fig. 1) and an aluminum alloy was poured into it. A violent bubbling action was noted as the steam forced its way through the molten metal. On breaking out the casting it was found that the cavity had only been partially filled in a manner which indicated that the pressure developed by the steam had restricted the flow of the incoming metal.

From this experiment it was concluded that the

Fig. 2—The test specimen (top) was poured in a moist plaster mold made without precoating the wax pattern, and had considerable metal penetration of the thin surface layer of plaster. A second test bar (bottom) was poured in a mold made from a precoated pattern.



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TABLE 1 - TENSILE TEST DATA OBTAINED WITH SPECIMENS CAST INTO INCOMPLETELY DEHYDRATED MOLDS (ALLOY 85)

| | Loss | Pro- | | | Spec | imen ! | No. | | | |
|------|------|------|------|------|------|--------|------|------|-------|------|
| | % | | 1 | 2 | 3 | | 5 | 6 | M | 0 |
| 1 4 | 40.9 | T.S. | 25.5 | 24.9 | 26.5 | 25.0 | 26.2 | 24.6 | 25.45 | 0.69 |
| | | E1. | 3.2 | 3.0 | 3.4 | 2.4 | 3.0 | 3. 3 | 3.0 | ~ |
| 4 4 | 40.3 | T.S. | 27.6 | 26.3 | 27.2 | 26.8 | 27.2 | 26.7 | 26.97 | 0.40 |
| | | E1. | 3.4 | * | | * | 3.2 | * | 3.3 | * |
| 6 3 | 37.1 | T.S. | 26.7 | 26.6 | 26.8 | 27.1 | 28.0 | 26.7 | 26.98 | 0.50 |
| | | El. | 3.8 | | 3.7 | 3.7 | * | 3.6 | 3.7 | |
| 6 3 | 32.3 | T.S. | 26.4 | 26.3 | 25.5 | 26.3 | 25.9 | | 26.11 | 0.33 |
| | | E1. | 3.4 | 3.6 | | 3.6 | * | | 3.5 | * |
| 7 2 | 29.3 | T.S. | 26.7 | 26.9 | 26.1 | 26.8 | 26.5 | | 26.6 | 0.28 |
| | | El. | * | 3.4 | 3.4 | | | * | 3.4 | * |
| 8 3 | 14.4 | T.S. | 27.5 | 27.2 | 27.0 | 25.7 | | - | 26.85 | 0.76 |
| | | E1. | 3.7 | 3.8 | | 2.9 | - | - | 3.5 | * |
| 9 | 39.4 | T.S. | 24.8 | 25.7 | 25.9 | 25.2 | 25.5 | | 25.42 | 0.33 |
| | | E1. | * | 3.8 | 3.1 | 2.8 | 3.1 | - | 3.2 | - |
| 10 4 | 10.7 | T.S. | 24.4 | 24.5 | 25.9 | 25.5 | 24.9 | | 25.04 | 0.58 |
| | | E1. | 2.9 | 2.7 | | 3.2 | * | * | 3.0 | - |
| 11 3 | 38.9 | T.S. | 27.8 | 27.9 | 28.9 | 27.5 | 27.8 | | 27.98 | 0.47 |
| | | El. | 4.0 | | 4.0 | * | 3.8 | * | 3.9 | - |
| 12 3 | 88.7 | T.S. | 27.7 | 27.5 | 27.0 | 26.9 | | | 27.13 | 0.10 |
| | | El | 3.0 | 3.0 | 3.1 | * | 2.8 | 2.6 | 2.9 | |

permeability of the standard investment was much too low. A more suitable material was found to be the permeable metal casting plaster described by Miericke and Johnson². This material was designed for use in plaster mold casting, not for investment casting. When mixed with water and stirred by a beating action a considerable amount of air is drawn into the mixture in the form of bubbles, which are stabilized by a foaming agent.

It is claimed, however, that when the prepared mix is poured against the patterns used in plaster mold casting a smooth layer of the material is deposited against the patterns. Thus the bubbles beneath the surface do not interfere with production of a good

surface finish on the final casting.

A batch of the permeable plaster was prepared and poured against the wax patterns in the same manner as is customary with the standard investment material. The mold was baked until the mechanically held water had been vaporized and only the chemically held water of the gypsum retained. An aluminum alloy was poured into the mold, using the suction casting fixture as before. No bubbling was noted, but the casting had the appearance shown in Fig. 2 (top). It is apparent that the metal had penetrated the thin surface layer and filled the spherical voids below the surface. These voids are the result of the bubbles incorporated into the mix by the foaming agent.

Refractory Pattern Coating Needed

In order to overcome the surface condition it was necessary to devise some procedure to precoat the wax patterns with a suitable refractory prior to the application of the permeable plaster. The most convenient material for this purpose was the standard investment. The precoating material was made up and the wax

pattern assembly was dipped into it, but the freshly prepared mix was found unsuitable because it was too watery in consistency and ran off the patterns, leaving bare spots. This was corrected by allowing the mix to stand until a thicker consistency was reached by the setting process of the plaster. It was then found that if immediately applied over the precoat the permeable plaster tended to wash away parts of the precoat, and it was necessary to first dry the precoat. On casting into molds, using the dried precoat technique, it was found that the precoat tended to spall, causing a characteristic type of failure on the surface of the casting. The spalling action indicated that the precoat was not being bonded to the back-up material.

Procedure Developed

This defect was corrected by sprinkling 20-30 mesh Ottawa sand over the surface of the still tacky precoat. The sand thus served as a keying agent in binding the precoat material to the permeable plaster back-up material.

The final procedure consisted of the following steps: (1) dip pattern assembly in ethyl alcohol to remove grease and dirt; (2) coat patterns with the precoating mixture of 100 grams investment per 48 cc water, waiting a few minutes until slightly thickened; (3) sprinkle 20-30 mesh Ottawa sand over the tacky precoat; (4) pour the permeable investment around the precoated patterns (100 grams per 75 cc water, mixing at 200-400 rpm in mechanical mixer for 2 min).

Figure 2 (bottom) shows a tensile specimen obtained from a mold prepared according to the foregoing procedure. Figure 3 illustrates a specimen mounted on a rubber sprue base. The specimen has been precoated, sprinkled with sand, and reinforced by a screen.

In Fig. 4 a sectioned mold shows the wax pattern as the dark portion surrounded by the white precoat. The precoat is surrounded in turn by the permeable investment, the porous nature of which is indicated by the numerous bubbles throughout the mass.

Properties of Tensile Specimens Cast in Wet Molds

After mastering the technique of casting into a wet mold, it was possible to determine the mechanical properties of aluminum when cast under these conditions. The amount of moisture retained in the mold could be controlled by the baking time and temperature. In order to determine the optimum moisture content for the best mechanical properties a number of tensile specimen molds were cast. The baking cycles for these molds were varied so that various amounts of moisture were retained. Tensile specimens from these molds were tested to determine ultimate strength and elongation. Standard 0.252 in. diameter, 1 in. gage length specimens gated from one end were used.

The alloy used in these tests was a 5 per cent silicon—4 per cent copper aluminum alloy. It was melted in a clay-graphite crucible which was heated in a 30-kw high-frequency converter. The casting temperature was adjusted to 1200F (650)C as measured by a chromelalumel thermocouple. The suction casting fixture was used with an absolute pressure in the surge tank of 10 psi, which is equivalent to a theoretical pressure of 5 psi on the exposed head of metal.

Table 1 lists the results obtained with varying

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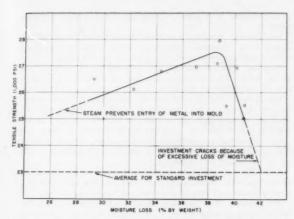


Fig. 5—Tensile data plotted to show the effect of moisture in the mold on the tensile strength of aluminum alloy No. 85. The molds were at room temperature.

amounts of residual moisture. Data shown in the table include the per cent weight loss of the mold due to the elimination of water in the baking cycle, the ultimate tensile strength and per cent elongation of the tension test specimens. Specimens cast into standard dry molds at the same pouring temperature used in the present work show tensile strength 15 per cent lower than the maximum shown in Table I.

Mold Moisture Affects Properties

The tensile data are shown in graphic form in Fig. 5. It may be noted that the maximum properties are obtained when the moisture content of the mold is reduced to correspond with a weight loss of 38-39 per cent. This weight loss was obtained by heating the molds for 12 hr at 300F (150C). The exact time and temperature of the drying cycle are not important since they will vary with the characteristics of the furnace used. Circulation of the furnace atmosphere is probably the most important single factor in determining the drying efficiency of a given furnace.

In spite of the scatter of the data, the general shape of the curve is readily apparent. On the left there is a region of such high moisture content that it is impossible to cast the metal into the mold without having the steam interfere with the entry of the metal. With less moisture, the casting is made successfully, but the properties are not as high as those recorded with somewhat drier conditions.

An optimum condition of moisture content is reached at which the mechanical properties are at a maximum. As drier and drier molds are tested the properties decline. There is a practical limit to which the molds may be dried, since the investment tends to crack when too much moisture is lost. For this reason it was not feasible to extend the curve sufficiently to the right to show its merger with the level characteristic of the standard dried molds.

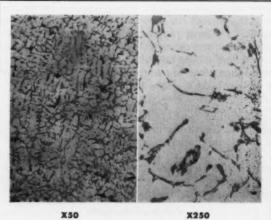
The microstructures of the tensile specimens were examined and representative areas are shown in Fig. 6 at 50 and 250 magnification. The microstructure of the specimen cast into the moist mold as compared with that of the specimen cast into a standard investment mold is considerably finer, and this is due to

the greater chilling power of the moist investment.

Although the technique presented in this report cannot be considered perfected as yet, it is possible to compare it in general terms with the standard investment method. The use of a precoat by the proposed method introduces a complication, but this is offset by a simplification of other parts of the procedure.

In the standard method it is necessary to use two vacuum treatments. The first treatment is used on the investment mix and the second on the flask containing the freshly poured mix. The volume of investment mixed in a batch is limited by the capacity of the vacuuming system and the setting rate of the investment. Using a precoat technique, only the precoat need be vacuumed, thus eliminating the second operation.

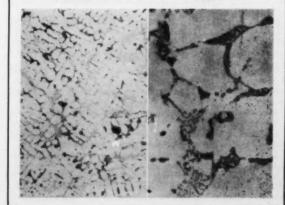
With a suitable precoat it should be possible to coat a large number of patterns at one time. Since the limitation of a vacuuming operation cannot interfere, it is possible to make up a large batch of the backing investment for filling many flasks at one time. The net effect seems to be an increased efficiency and time saving in spite of the added predip operation.



FROM MOIST MOLD

Fig. 6—Microstructures of aluminum alloy tensile test specimens cast in a moist mold and standard dried mold. Etchant, Fe (NO₃)₃. X50 and X250.

FROM STANDARD DRIED MOLD
X50 X250



The thermal treatment of the molds, usually referred to as the "burnout," is an inexpensive operation compared with the treatment required with the standard investment. It may be done overnight at a low temperature, such as 300F (150C), or may be accomplished in a few hours at higher temperatures. An incidental saving during the casting operation is found in the fact that the moist investment causes the metal to solidify more rapidly, thus increasing the output of a given casting fixture.

In matter of cost, the new technique enjoys a marked advantage over the standard investment. Neglecting the cost of precoat, which is slight in any event, the cost of the backing investment is only a fraction of the cost of the standard investment. Taking into consideration also the fact that backing investment has half the density of the standard material, there is an even greater potential saving in investment material.

In precision casting the cost of the investment is determined to a large extent by the size of the casting. Small castings use little in the way of investment materials, and the major part of the cost is in the labor expended in making the part.

With large castings, the cost of the investment becomes an important factor in the price of the casting, since the investment consumed in making the casting represents a large proportion of the total cost of the part. Thus, any means by which investment costs can be reduced should stimulate the use of the method for large castings which are not economically feasible at present because of the high investment cost.

Conclusions

1. Retained moisture in investment molds causes a significant improvement in the mechanical properties of aluminum-copper-silicon alloys.

2. Molds prepared by the technique described in this report are less expensive than standard type molds.

3. Burnout of molds is simpler and less costly than with the standard investment material.

Acknowledgment

The authors are indebted to the Department of the Army for permission to publish this report. The opinions expressed in the paper are those of the authors and are not necessarily endorsed by the Department of the Army.

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DISCUSSION

Chairman: L. E. GRUBB, International Nickel Co., Bayonne, N. J.

Co-Chairman: T. E. Moore, Ransom & Randolph Co., Toledo, Ohio.

PAUL SOLOMON: 1 In regard to the precoat, is your precoat water soluble? Does backup have a tendency to disalve precoat?

MR. Lipson: We allowed the precoat to setup completely and

MR. LIPSON: We allowed the precoat to setup completely and then it was not affected by the backup.

K. A. MIERICKE: ^a In regard to the question of setting and adhering of the materials discussed by Mr. Lipson, the investment material used contains approximately 15 per cent silica fleur. It adheres readily to any other plaster or silica investment. In fact, this investment will absorb moisture out of the silica investment.

W. P. O'Brien: Was it absolutely necessary to pour the mold

in the vacuum to eliminate the steam or did you try any other method?

Mr. Lipson: We used a suction casting device in our experimental work. However, there is no reason to believe that a centrifugal casting device would not produce the same result.

MEMBER: The author said that he removed 38 to 40 per cent of the water. What he meant was that he removed 38 to 40 per

cent of the weight of the mold.

Mr. Lipson: The 38 to 40 per cent figure represents the weight less of the mold after completion of the drying cycle. The moisture remaining in the investment may be obtained by subtracting the weight less from the total amount of water used. This indicates that practically all of the free water is removed and that the retained moisture is the water of hydration taken up by the setting of the calcium sulphate.

D. R. Hayes: 4 What are the limitations on casting temperature in your process? What is the difference between the thickness of your precoats and that of the standard investment casting?

Mr. Lipson: The practical temperature limitation for these molds is approximately 1400 F. The chief utility is therefore for aluminum castings. We have made brass casting, with pouring temperatures up to 2000 F, but it was necessary to dehydrate almost completely the mold at which point cracking tended to occur.

CHAIRMAN GRUBB: What is the thickness of your precoat compared to the others?

Mr. Lipson: The thickness of the precoat was between 1/32 and 1/8 in. To prevent thinner coats, the precoat mix was allowed to thicken or set up to a certain extent before dipping the patterns into it. Of course, this operation had to be done in a limited time before the mix became too thick for application.

E. I. VALYI.⁵ Would it not be conceivable to build up the precoat shell somewhat heavier, say to 3/32 in. and then to omit the backing altogether to do the dewaxing and to use the shell alone, with a metal powder or a coarse sand or something of that sort as backing? In other words, use a technique somewhat similar to the Croning process, thereby getting higher heat conductivity in the molds.

MR. LIPSON: That technique had not occurred to me but it is quite possible that it could be done. There is one thing that I would be a little concerned about and that is the strength of the precoat shell and whether it would stand up during dewaxing without the aid of the backing. Also, elimination of the backing would remove the source for the moisture which provides the chill for the casting.

MR. MIERICKE: Possibly I may be able to clarify the discussion regarding the amount of water removed during the dry-out and calcination. Not knowing the water-investment ratio used by the author I will have to cite standard practice. The usual proportions used in mixing this investment are 100 lb of investment to 80 lb of water. This ratio is normally referred to as "80 consistency." This ratio or consistency can be varied to change the fluidity of the slurry.

After the plaster investment and water have been properly mixed the investment will hydrate or set. About 18 per cent of the water used chemically combines with the plaster in setting. This ratio of water is normally referred to as the water of crystallization. Any amount of water above the 18 per cent is considered free water, or the water required to produce pourable slurry.

In drying the molds preparatory to casting metal, all the free water must first be removed before calcination can take place. The free water amounts to approximately 36 per cent of the wet weight of the mold. If the mold is to be calcined or the chemically combined water removed, higher temperatures are required. The mold weight must then be reduced to approximately 46 per cent of its original wet weight. In practice, providing the permeability is high enough to release gases formed by the contact of the mold surface with the hot metal, only the free water, or the 36 per cent, should be removed. This practice will produce stronger, more accurate molds, having a greater chilling rate.

The ratio quoted above should only be used as an example and may not be true in this case, since a precoat of silica investment was used.

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² Soles Mgr., Baroid Sales Div., National Lead Co., Chicago.

⁸ Works Mgr., W. D. Allen Mfg. Co., Bellwood, Ill.

⁴ Engineer, Pratt & Whitney Aircraft Co., East Hartford, Conn.

⁶ A.R.D. Corporation, New York.

METAL MELTING

Application of Thermodynamic Principles to Melting Nonferrous Metals

By Robert I. Moore*

ABSTRACT

While operating crucible furnaces at Wilson Brass and Aluminum Foundry Co. of Toronto, and later during preparation of a thesis on "Design and Operation of Oil-Fired Crucible Furnaces," the writer became interested in reducing high losses that occur in conventional metal melting methods.

The low efficiency achieved by conventional crucible furnaces justified an analytical approach to the problem. Since a large percentage of the heat supplied by the fuel is wasted in the heating process, a method determining the causes of individual losses and their extent is used.

The heat losses occurring in the furnace due to the thermal processes of radiation, conduction and convection are dealt with individually.

While considerable losses result from radiation and conduction, the largest portion of heat lost is that heat rejected in the exhaust gases. By calculations based on the Carnot Efficiency Ratio, ultimate efficiency possible can be determined for any condition of flame temperature and exhaust gas temperature. On this basis it has been found that ultimate efficiencies of 10 to 15 per cent are usual. Radiation and conduction losses reduce these figures to 3 to 10 per cent. Consequently, 90 to 97 per cent of the heat in the fuel is lost in the melting process.

Consideration is given to means whereby higher efficiency could be achieved, and actual efficiencies resulting from proposed conditions are calculated.

THE USUAL METHOD OF MELTING METALS in a crucible furnace is an inefficient process. This is apparent to users and operators of crucible furnaces. However, determination of actual efficiencies reached, and consideration of means whereby efficiencies could be improved, is a study that has not resulted in noticeable changes in furnace design for many years. Improvements that have been achieved have been expressed with respect to previous operating conditions, and have not been considered in the light of the ultimate efficiency possible for given conditions. A new approach to the problem is therefore justified. This approach must be made by an observer who is sufficiently familiar with the problem to realize the requirements of the job and the restrictions imposed by the nature of the work, and who can ignore conventional practices that have evolved through habit and not from necessity.

Nature of Heat

Before attacking the problem of metal melting, some consideration should be given to the nature of heat and temperature. It must be remembered that heat is a form of energy, and is therefore subject to the law of conservation of energy—that energy can be neither created nor destroyed. The fact that this law has been considerably modified by modern science need not concern the reader here, since the law can be rigidly applied within the limits of the problem. It can therefore be stated that the best energy available is equivalent to the energy latent in the fuel. When this energy is liberated by the combustion process, it can be used to increase the temperature of the metal being melted, and the heat not utilized in this way is lost.

The efficiency of the melting process is an expression of the energy used to heat the metal, with respect to the total energy supplied by the fuel. It should be pointed out that heat added to metal may not result in an increase in temperature if the metal is at its melting point. Heat energy is required to effect the change of state from the solid to the liquid, and this change takes place at a constant temperature in the case of a pure metal. Heat added to the metal should therefore be thought of as energy used to raise the temperature and to melt the metal. Energy so used will be referred to as "heat added" as distinct from "heat lost."

The relation between heat and temperature should be kept clearly in mind. While the heat of a body is almost proportional to its temperature, it does not follow that the hottest body contains the most heat. A red-hot nail is at a considerably higher temperature than a tub of boiling water, but the boiling water contains much more heat than the nail. The time required to heat the water and to heat the nail over a gas flame is proof of this statement.

Another characteristic of heat to be considered is that heat will not flow up the temperature scale. If in any process a given temperature is required, the source of heat must be in excess of this temperature. To use the example of the nail and the water, although the water contains more heat than does the nail, boiling

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^{*} Wilson Brass and Aluminum Foundry Company, Toronto, Student, Department of Mechanical Engineering, University of Toronto, Toronto, Ontario, Canada.

water could not be used to heat nails to red-heat. For this process, heat must be available at a temperature in excess of that of a red-hot nail.

Heat Transfer

Heat may be transferred from one body to another by any or all of three ways: radiation, conduction and convection. Radiation is the exchange of heat energy from one body to another by waves passing through a separating medium. The medium is assumed to support the wave motion, but is otherwise unaffected by the passage of the heat. The earth receives all of its heat from the sun by radiation. Conduction occurs when heat energy is transferred from one molecule to the next within a substance, as when the handle of a poker becomes hot after the tip has been in the fire for some time. Convection is the transfer of heat energy from one place to another by the motion of a liquid or gaseous medium. The motion is usually caused by the disturbance of the uniformity of the density of the medium, which is the cause of smoke rising in a chimney. For the problem in hand, the term convection will also apply when the motion of the fluid or gas is due to other causes.

Heat Transfer to Metal Charge

Heat energy can be transferred to metal in a furnace by a combination of these three heat processes. In practice, the effects of radiation and convection are negligible, and almost all heat added to the metal is the result of conduction through the wall of the vessel containing the metal and through the metal itself. The rate of heat transfer by conduction is a function of five factors:

- 1. Temperature of the metal
- 2. Nature of the metal
- 3. Temperature of the heat source
- 4. Nature of the separating medium
- 5. Thickness of the separating medium

1. Temperature of the metal will depend on the stage of the process. As the metal reaches its pouring temperature, the rate of heat transfer will decrease.

2. Heat required to raise 1 lb of metal 1 deg F varies with different metals and alloys, as does the heat required to effect the change of state from the solid to the liquid form These conditions are imposed by the nature of the metal and must be considered as constant factors in any melting process.

3. Rate of heat transfer is almost proportional to the difference in temperature between the body being heated and the heat source. Therefore, the higher the flame temperature used, the faster heat will be transferred to the metal. The maximum temperature used will be governed by the fusing temperature of the refractories used, and by the effects of elevated temperatures on the metal being melted.

4. The separating medium must have refractory qualities of a degree sufficient to withstand the flame temperature used, and should at the same time have as high conductivity as possbile. Resistance to chemical attack and physical abuse must be characteristics of the separating medium, but these qualities do not affect the problem of heat transfer.

5. Since greater conductivity would result from re-

ducing the wall thickness of the containing vessel, choice of wall thickness must be a compromise between the requirements of heat conductivity and physical strength.

Heat Losses

An indirect approach to the problem of metal melting would be the study of heat losses that occur. Because lost heat represents a sizable portion of the heat supplied by the fuel, this approach is justified. Heat is lost by the same thermal processes by which heat is added to the metal—radiation, conduction and convection—and each process contributes considerably to lost heat. For the purposes of study, however, the lost heat may be considered under four headings:

- 1. Radiation
- 2. Conduction
- 3. Stored energy
- 4. Convection.
- 1. Radiation—That radiation losses are considerable may be illustrated by a simple experiment. If a radiation pyrometer, or an unprotected portion of the

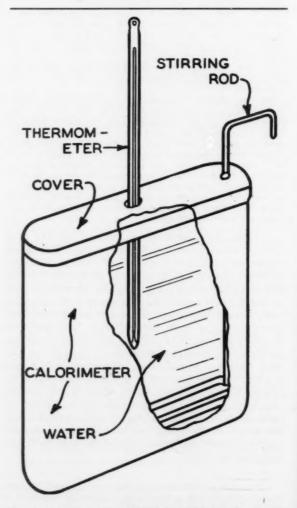


Fig. 1—Calorimeter used for determination of emissive powers of surfaces.

body is exposed to a hot furnace wall, and an insulating shield is alternately interposed and removed, the effects of radiation will be apparent. More involved experimentation would indicate that the amount of radiation taking place is a function of two factors:

a. The temperature difference between the radiating surface, and the surrounding medium.

b. The characteristics of the radiating surface. Because radiation is dependent on these factors only, experiments may be conducted using simplified apparatus to determine the conditions that would result in reduced heat loss. Experience has shown that smooth, bright, metallic surfaces radiate approximately two-thirds the energy radiated by dull, black, nonmetallic surfaces.

The writer carried out a simple experiment to illustrate this fact. Two copper calorimeters of a special shape (Fig. 1) and provided with covers and stirrers were used. One was polished (nickel-plated) and one was blackened. The calorimeters were of equal weight. Equal portions of water, heated to 158 F (70 C) were added to each calorimeter. The temperature of each calorimeter was recorded every 2 min for a period of 74 min. The temperature of the room was also noted during the experiment, and the water in the calorimeters was stirred often to ensure even temperatures throughout. By arranging the data in the form of a graph (Fig. 2) an expression of the rate of cooling with respect to the excess temperature was calculated. It can be shown that the rate of cooling is approximately proportional to the excess temperature of the body over the surrounding medium (Newton's Law of Cooling).

Extensive experiments by Stefan indicated more

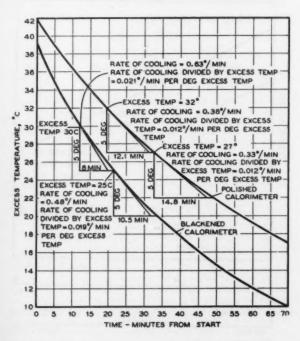


Fig. 2—Comparative rates of cooling with respect to excess temperature, for polished and blackened calorimeters.

accurately that the rate of cooling is proportional to the fourth power of the excess temperature. In any case, a comparison of the rates of cooling of the two calorimeters shows that the ratio of emissive powers to the polished and blackened surfaces is approximately 2:3.

This conclusion would suggest the use of bright metallic surfaces on outer furnace walls.

2. Conduction—A large amount of heat is lost by conduction through the furnace wall, base, and lid. Fortunately, materials possessing refractory qualities to a high degree are generally good heat insulators. There is, however, considerable variation of the coefficients of conduction among materials in this group, and those possessing the highest degree of refractoriness are not the best insulators. The logical selection of furnace wall material would therefore be a combination of refractories each possessing particular characteristics, and this procedure is usually followed by furnace builders.

Extensive experiments have been conducted to determine the coefficients of conduction of many refractory materials and this data is available in handbooks.

3. Stored Energy—In normal operation, the refractory walls of a furnace will absorb heat until a maximum operating temperature is reached, and this heat will be lost in cooling when the furnace is shut down. Some operators attempt to reduce this loss by melting metals of lower pouring temperature at the end of the working day. While this practice is advisable, only a small portion of the heat stored in the furnace walls is saved.

The amount of heat stored in the furnace wall depends on the specific heat of the refractories used, and the thickness of the wall. The choice of refractories should depend on the factors discussed under Conduction, since the variation of specific heats is not large. The amount of heat stored will therefore be dependent on the wall thickness. But heat lost by conduction is directly proportional to its thickness. Therefore the wall thickness should be that which will result in the

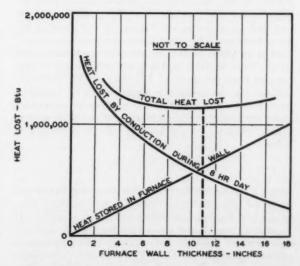


Fig. 3-Graphs of heat stored in furnace wall, and heat lost by conduction during an 8-hr day.

lowest overall heat loss considering both conduction and storage.

The graph in Fig. 3 is based on possible conditions, but does not necessarily apply to all conditions or even average conditions, since these will vary from foundry to foundry. The assumptions made are:

a. Eight-hour operating day,

b. Furnace temperature 2800 F (1538 C).

c. Average specific heat of refractory wall = 0.28

Btu per pound per degree Fahrenheit.

d. Average coefficient of conduction = 25 Btu per square foot per hour per degree Fahrenheit per inch thickness.

e. Temperature of outside of furnace wall = 400 F $-10 \times$ wall thickness in inches.

f. Average refractory temperature = 1/2 (2800 F + temperature of outside of furnace wall).

g. Average bulk density = 150 lb per cubic foot. The graph indicates that for these conditions the minimum overall loss would occur with a furnace wall 10½ in. thick. However the overall loss curve is fairly flat in this region, and the interests of economy of material and space would be served by choosing a wall thickness somewhat less than the calculations indicate.

4. Convection—As the mixture of fuel (oil or gas) and air enters the furnace, combustion takes place. If ideal conditions exist, all of the energy in the fuel will be converted to heat, raising the temperature of the gases to a degree dependent on the temperature of the air and fuel supplied, the pressure in the furnace and the heating value of the fuel. If perfect combustion takes place, the hydrocarbons in the fuel will combine with oxygen from the air to form water vapor and carbon dioxide, and heat energy will be liberated.

As the hot gases pass through the combustion space in the furnace, heat is given up to the furnace walls and to the metal. Thus the gases are cooled and leave the furnace at a lower temperature than that of the flame. If the specific heat of the gases is assumed to be constant over this temperature range, then the absolute temperature of the gases will be in direct proportion to the heat content per unit mass. With respect to the fuel and air flow, the mass flow of the fuel and air entering the furnace is the same as the gases leaving. Therefore flame temperature can be used as an index of heat supplied to the furnace, and temperature of the escaping gases as an index of heat rejected from the process. Efficiency of the furnace as a devise for absorbing heat supplied can be expressed by the ratio of heat absorbed to heat supplied. By using the known temperatures as indices of these heat values, efficiency of the furnace can be expressed as follows:

$$\text{Efficiency} = \underbrace{\frac{T_{\tau} - T_{2}}{T_{1}}}$$

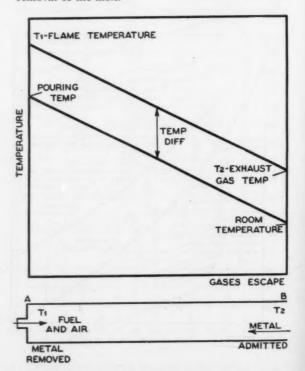
where T_1 is the absolute temperature of the flame, and T_2 is the absolute temperature of the escaping gases in degrees Rankine (absolute temperature scale). If T_1 and T_2 were found to be 3260 R (2800 F, 1537 C) and 2860 R (2400 F, 1317 C) respectively, ultimate efficiency possible would be 12.2 per cent. The losses already considered plus a loss occasioned by the reduction in pressure through the furnace would reduce this figure to a lower overall efficiency.

If the furnace design were such that the temperature of the escaping gases, T₂, was reduced to 1460 R (1000 F, 537 C) the resulting ultimate efficiency would be 55.2 per cent, and the corresponding fuel saving would be 78 per cent. Losses resulting from radiation and conduction usually encountered would not reduce this percentage saving, since the same losses affect the furnace operation under the conditions resulting in 12.2 per cent efficiency.

It is at once evident that hot gases at 1000 F (537 C) would be of little use in melting metal whose pouring temperature is in the vicinity of 2100 F (1150 C); at least at no time after the temperature of the metal had reached 1000 F. This of course is due to the fact that heat will not run up the temperature scale. However these hot gases could be easily used for heating

the charge up to 1000 F.

These considerations suggest melting of metal employing a flow process in which the maximum temperature exists adjacent to the metal at or near its pouring temperature. The products of combustion would be directed through the system opposite to the movement of the metal, and the gases would escape at the lowest temperature feasible at the point where cold charge metal is introduced. Such a system is illustrated schematically in Fig. 4. Combustion occurs at A where temperature T_1 is reached, and the gases move to the right being cooled by contact with the metal that is moving to the left. The gases finally are discharged at B at temperature T2. Cold metal charge is introduced at B, and the metal moved to the left, being heated by the gases and finally reaches its pouring temperature at A, where provision is made for removal of the melt.



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Fig. 4-Schematic illustration of proposed melting process.

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The system might be a continuous flow process as suggested by the illustration, or the charge might be introduced in unit quantities, i.e. in crucibles. The disadvantage of the latter system is the necessity of adding charge metal to the crucible as melting progresses.

Metal melting furnaces have been designed that embody this principle in a limited sense. While these developments are noteworthy improvements over the conventional furnace, and represent the most scientific approach to metal melting with fuel oil or gas, they leave much to be desired. The continuous flow process and the unit quantity method have been used, but in both cases, the temperature gradient has been reduced to essentially two temperature levels; i.e., T₁ is the furnace temperature and T₂ is the preheat temperature. In both cases, the minimum limit of T₂ is the practical preheat temperature which is of necessity considerably higher than the temperature of useful hot gases.

Conclusions

Selection of furnace wall materials, wall thickness, flame temperatures, and furnace wall surface can be made to insure most efficient operation of the furnace for given conditions of exhaust gas temperature. However, losses resulting from conduction and radiation are small compared to loss occasioned by rejection of products of combustion at relatively high temperatures.

This fact justifies investigation of the possibility of utilizing exhaust gases for preheating purposes down to temperatures within two or three hundred degrees of standard atmospheric temperature. Extensive experiments would have to be carried out to reach the economic compromise between the theoretical concepts established and their practical application.

DISCUSSION

Chairman: J. J. Curran, Walworth Co., Greensburg, Pa. Co-Chairman: A. K. Higgins, Allis-Chalmers Mfg. Co., Milwaukee.

V. PASCHKIS (Written Discussion): ¹ The new melting technique mentioned towards the end of the paper should, if developed, be of considerable interest. It would represent one method of utilizing the heat content of waste gases without going to the expenditure of building recuperators or regenerators. It would be desirable that the author disclose more of the design of this experimental furnace.

Some of the statements in the early parts of the paper, however, need modification.

The author writes of four items of heat losses. It would be more correct to use the following concept.

The heat losses should be counted from the hottest point in the furnace which in the case of crucible furnaces obviously is the flame. The heat losses occur in part through the walls of the furnace chamber and in part through the crucible through the charge and through radiation and convection through the top of the charge. The latter seemed to be disregarded entirely although they contribute considerably to the total heat less es.

The conclusion in the Section "I. Radiation" is theoretically correct to a limited extent, but practically insignificant. The heat less by radiation and convection from the outside surface of the lining depend on the outside wall temperature, which in turn is influenced by lining thickness and material. In view of the use of insulating refractories, the change in rate of heat

losses by change of surface paint is negligible. This can easily be proved by calculations shown in Trinks¹ or in the book on Electric Furnaces² by the writer.

The calculation of the stored energy which the author puts uncritically in juxtaposition with the wall losses depends on the wall design and on intermittency. A fairly accurate calculation can be made based on charts published by the writer. In the paragraph on stored energy, there is also the misleading statement that the heat lost by conduction "is directly proportional" to the wall thickness.

The losses through the furnace wall and from the top of the bath have to be increased because of incomplete combustion. This section in the paper is misleadingly called "convection." The energy lesses due to incomplete combustion can be computed for example showing a technique indicated by Rossin.

The criticism in the several last paragraphs should not detract from the merit of the paper in an attempt to study the thermal happenings in a melting furnace and to apply the principle of utilization of waste gases as indicated in the end of the paper.

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2. V. Paschkis, "Industrial Electric Furnaccs," Interscience Publishers Inc., vol. 1, 1945, vol. 2, 1948.

3. C. B. Bradley, C. E. Ernst and V. Paschkis, "Economic Thickness of Thermal Insulation for Intermittent Operation," *Transactions, ASME*, vol. 67, p. 93 (1945).

V. Paschkis, "Economic Thickness of Cylindrical Insulation," preprint 49-F-27, presented at the ASME Meeting in Erie, Pa., Sept. 1949.

4. P. Rossin, "Waste Heat Recovery From Industrial Furnaces," Chapman and Hall, 1948.

Co-Chairman Higgins: There is one point which has not been sufficiently emphasized. We are talking about what sound like rather small improvements. The difference between 2 per cent and 15 per cent does not sound like a great deal. Actually the improvement is one of ratio rather than percentage. If we go from 2 per cent to 4 per cent, we use half as much fuel. So when we speak of improvements three and four times basic efficiency they become well worth while.

MEMBER: We increased the oil pressure on an oil-fired furnace and reduced the amount of air necessary to atomize the oil. We were able to cperate with very little excess air against a standard practice of about 5 per cent excess air. Our results have not been completed but it appears as though we are able to cut oil consumption almost in half. This seems unbelievable so we are rechecking it.

H. J. Roast: ⁸ Could not some use be made of these hot waste gases in the winter time? We have asked our engineers to try that, but they have not yet submitted to us what we thought sufficiently practical ideas to warrant the expenditure. Perhaps the author might have some ideas along this line. I would also like to ask if the author could give us the thermal efficiency of the modern steam engine, just as a matter of comparison.

MR. MOORE: I have given some thought to the utilization of hot exhaust gases for heating the foundry of adjoining buildings. The only argument against this idea was that it was practical during the winter menths only.

A suggestion has been made of using this heat to heat water for shower purposes. That is a possibility. I think possibly something of that sort might easily be done, probably more readily than using the heat even to heat the building because the combustion of the gases would necessitate a heat transfer to pure air or to water or some other medium to heat the building and transferred again into the air in the working area.

In answer to the second question relative to efficiency of steam and gasoline engines, I understand that efficiencies as high as 35 per cent have been attained in a turbine. The efficiency of the modern gasoline engine probably is somewhat lower.

DR. PASCHKIS: The efficiency of 33 per cent is correct for large power plants but cannot be obtained in small plants. Gasoline engines may reach the efficiencies of 30 per cent as maximum in aircraft engines; 20 per cent is a good average value. Diesel engines sometimes reach 35 per cent, but 25 to 30 per cent is a more nearly average value.

CO-CHAIRMAN HIGGINS: That is true. You have to go to the

¹ Technical Director, Heat and Mass Flow Analyzer Laboratory, Columbia University, New York.

² Consultant, Westmount, Que., Canada,

80,000 kw stations in order to get efficiencies on the order of

32 to 35 per cent.

G. P. HALLIWELL: ^a I would like to bring up the point of waste heat utilization. It is not economical to rob Peter to pay Paul. In the smelting industry, we have often been asked, "Why don't you use the waste heat in a recuperator or regenerator?" One of the disagreeable things about the brass industry is that you are dealing with a metal, zinc, which is quite volatile and produces copious zinc oxide fumes when in contact with air at high temperatures. If waste gases containing this zinc oxide are passed over pipes to heat water, zinc oxide deposits on them with consequent loss of heat transfer. This deposit must be removed and involves labor. Whether it is cheaper to heat water by waste gases with its attendant cleaning costs, or by an external heat source, is a question that can be answered only by experience.

R. C. Tibbetts: ⁴ Our foundry consists of a large, long building with a monitor type roof and no partitions between different departments. We formerly ran six Walker York furnaces which heated our foundry in the wintertime. The open monitor roof cooled it in the summertime. About two years ago we installed Ajax Hi-Frequency Furnaces, and that was the first step in reducing heat loss. We have no heat in the foundry now at all. We had to install steam heat throughout the plant. We cool our coil with water. I do not believe we would take much heat from the furnace in that method. Then we cover the top of our metal with charcoal which holds the heat down and reduces our melting time by about 3 or 4 min. Now we have practically no heat loss at all in our furnaces.

MR. MOORE: The foundries I visited did not use electric furnaces. They used fuel oil furnaces. I do not know what the efficiency of an induction furnace is.

CHAIRMAN CURRAN: It is on the order of 50 per cent.

MR. Moore: I was basing my assumption on other electrical appliances. It seems to me there is less room for improvement in the electrical device than there is in the oil-type furnace. I have not given consideration to electric furnaces of any sort, because I did not have the opportunity of working with them.

MR. TIBBETTS: The only radiation of heat that we get would be in the crucible in that we do not heat the crucible, we heat only the metal and the crucible will absorb a certain percentage of that heat. But on red bronzes where you might run as high as 2200 or 2300 F in pouring or melting, our crucible never turns red. It stays black.

MR. MOORE: Is the furnace area not hot?

MR. TIBBETTS: No, the top of the furnace is cold enough to

put your hand on at any time during the melt.

Mr. Moore: Suppose an oil-fired furnace were built to run on one-fourth of the fuel, would it be greater competition for an electric furnace?

Mr. TIBBETTS: We melt much faster.

Mr. Moore: I am looking at the problem strictly from an economic standpoint. Suppose it costs a given amount to melt metal by electricity and a similar amount to melt it with oil, then if we would devise a system whereby we could cut the cost by half, that would be an advantage.

MR. TIBBETTS: When melting bronze with coke as the fuel we figured our fuel cost was a cent per pound of metal melted. This compares with 4½ to 5 mills per pound of metal melted in an

induction furnace.

MEMBER: You have a higher original investment in the induction furnace to attain that lower melting cost.

MR. TIBBETTS: That is true.

We use a No. 70 lift coil and a No. 650 tilting furnace. It can melt 800 lb of bronze at 2250 F in 1 hr. The lift coil will melt in a No. 70 crucible 240 lb of bronze in 19 to 20 min at 2200 F.

MR. MOORE: That is better than any oil furnace I have. MR. TIBBETTS: We have no gassing problem now.

MR. Moore: You do have the heating problem though.

Mr. Tibbetts: Yes, we have to steam heat the foundry.

G. K. Eggleston: 5 I did considerable experimental work about ten years ago on this subject. As I recall we discovered that the greater portion of the heat that finally gets into your metal is that reflected by radiation from the hot wall of the furnace to the crucible. We found the factor that helped most was to do whatever is necessary to keep the furnace wall hot and insulation was one of the elements by which this can be accomplished. I remember after giving a paper on this subject one of the foundrymen stated that he had heard that insulation was good for a furnace so he put some in. He said the only trouble was that after two heats he melted the lining out of the furnace so he discontinued use of insulation. Had he stopped to consider that if the lining had stood up he would have melted a lot faster or if he had cut the fuel back he would have melted just as fast as he did in the past because his wall temperature would have been where it was before.

We also discovered that the rate of fuel input can be very important if you have a furnace wall that will stand the temperature. In one instance we doubled the fuel input and reduced our melting time to one-fourth of what it had been

our melting time to one-fourth of what it had been.

L. J. Andres: In answer to the question of heating the building with hot exhaust gases, we have installed a little system that has done that very nicely for us. We have a hood over each melting furnace that runs into one central trunk line and that in turn runs out through the roof. We put a jacket around that. The jacket also runs out through the roof and fresh air comes down through the outer jacket around the hot stack. About a 20-in. diam, 4-blade fan with a 14-hp motor runs all day. It heats the foundry in the coldest weather. The system costs about \$2,000 to install and we figure it saves us \$700 in fuel a year.

MEMBER: We melt in a No. 80 or No. 90 crucible oil-fired furnace that we made ourselves; we also made our own burners. We melt 250 lb of red brass in 18 to 20 min in this furnace.

MEMBER: How thick was the lining in the crucible?

MEMBER: It is 3 or 4 in. thick.

MEMBER: Do you keep a cap over the top of the crucible? MEMBER: No we do not. We have a cover on the furnace.

MEMBER: I would like to ask Mr. Tibbetts about melting in his induction furnaces. Are the figures you gave the cost of melting in the induction furnace alone or is that the average cost?

MR. TIBBETTS: That is our power cost. It will vary throughout the country because of the variation in power rate in other parts of the nation. I did not mean that we were setting any record by melting 240 lb in 19 min. In our foundry, using coke as the fuel, we used to melt the same amount of metal in 50 min. So we feel we have greatly reduced that time. In the two years of operation to date we have had no replacements or maintenance cost on the furnaces at all. We are still using the same lining and it has not been patched. A clay-graphite crucible averages between 100 and 150 heats. The only abuse that the crucibles get is the normal wear and tear in the interior due to abrasion from our shanks.

MR. ROAST: I would like to congratulate Mr. Moore on his paper. I feel that as time goes on we foundrymen have to pay more attention to the little items of cost. We are prone to suffer from lack of inertia and do not want to be bothered with thermodynamics. When the thermodynamics are translated into foundry language, which they will be, then they will be valuable. I should like to see more papers of this type because I think in the end we will have to come to it.

³ Director of Research, H. Kramer & Co., Chicago.

⁴ Vice-President, Wollaston Brass & Aluminum Foundry, Inc., No. Quincy, Mass.

Vice-President, Nonferrous Perma Mold Inc., Mansfield, Ohio.
 Vice-President and General Manager, Lawren Foundry Co., Milwaukee.

MANGANESE RECOVERY IN ACID ELECTRIC STEELMAKING

By Sam F. Carter*

ABSTRACT

Investigation of factors affecting manganese recovery by correlating concurrent metal and slag analysis before and after
manganese addition is presented. Data were collected on acid
electric (few basic) heats over a period of five years. Extremes
in slag composition were artificially created by draining off the
original slag and adding synthetic slag mixtures of various predetermined ratios. Conditions investigated were single slag
heats of normal manganese content, single slag heats of higher
Mn content, double slags low in FeO, high in lime, and high in
MnO. Factors studied were slag composition, desired metal
analysis, temperature, time, slag fluidity, quantity of slag, and
ladle additions. Some effects of use of oxygen on the various
factors.

Application to better Mn recovery and improved Mn control.

Introduction

Manganese is recognized as an essential alloying element in all steels because of its effect on inclusion form, the properties of the matrix, and hardenability in heat treating. Producing a final manganese content within a specified range is one of the responsibilities of the steelmaker, regardless of process. Because of its reactivity in the acid furnace, preliminary analyses for Mn are of no value and its addition is withheld until a few minutes before tap. Control is generally accomplished through empirical allowances made by the melter on the basis of repetitive experience. Better control of this element as required by higher quality heat treated steels and the flexibility to produce special non-repetitive types of steel requires prediction of manganese loss on a more scientific basis.

Mn Reaction in Basic Furnaces

A considerable volume of data has been published on the Mn reaction in basic furnaces. Less quantitative information has been published on acid furnace experience. Contributions to the fluidity-composition relationship have been made by several investigators, 1.2.3,4.5 However, the chemical changes experienced on various types of steels have not been quantitatively accounted for to the satisfaction of both the physical

chemist and the practical melter; and the factors affecting the reactions have not been explained sufficiently to permit a scientific approach to the practical problems of improved Mn control. The purpose of this paper is to present some data collected over a period of several years which seem to make some contribution toward a better quantitative understanding of the reactions causing manganese loss in acid steel making.

On approximately 100 heats, samples of both metal and slag have been taken before the addition of manganese, at intervals after its addition, and after the heat had been tapped into the ladle. Changes in slag composition were correlated with changes in metal composition. Metal temperatures were determined with an optical pyrometer of the disappearing filament type. The quantity of slag was determined on a representative number of heats by weighing the slag dumped from the ladle after the heat had been poured. This procedure was followed on a number of types of carbon steels for castings and on several furnace practices. Unusual slag conditions were given special attention. Some extremes in slag composition were artificially created by draining off the original slag and adding synthetic slag materials of various predetermined ratios. This made it possible to exaggerate the individual effect of each slag component.

Discussion of the results will be divided according to the several factors as they are studied in the progressive development of the subject. Most of the principal analytical data is contained in Tables 1, 2, and 3. Essential parts of these data will be repeated as each heat is discussed.

Normal Mn Range—Single Slag Heats

The chemical changes that take place in the metal and slag of an average single slag acid heat are typified by Heat 1 and 2 in Table 1. To Heat 1 an addition of 0.95 per cent Mn was made. A residual of 0.15 per cent was present after oxidation of the charge. The heat was tapped 2 min after the addition at approximately 3270 F. The sample from the ladle analyzed 0.63 per cent Mn. Actually the equivalent of 0.48 per cent Mn was lost from the metal along with 0.14 per cent Si.

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^{*} Assistant Melting Superintendent, American Cast Iron Pipe Co., Birmingham, Ala.

TABLE 1-ACID ELECTRIC HEATS

| D | | | , | 2 20 | | 3 | | E | | 3 | | | Slag C | Slag Composition, | n, % | |
|---------------------------|----------------------------|----------|---------------------|--------|-----------------------|----------------------|-------|-----------------|------------------------------------|-------------------|----------------------|-------|--------|----------------------|-------------------|---------------------|
| | Residual | al Added | fin % Min ed Tap | Final | In % Mn Recovery R | n % Si y Recovery | Ladle | Tart Temp.,F | Time | Slag Vis., in. | SiOs | A1203 | Fe3 | MnO | CaO | MgO |
| Regular | 0.15 | 0.95 | 150 | 0.62 | 59 | -0.14 | 0 | 3270 | Before Mn | 5.0 | 53.9 | 2.3 | 18.8 | 17.5 | 3.3 | 2.3 |
| | 0.12 | 1.15 | 10 | 0.77 | 09 | -0.10 | Al | 3180 | Before Mn | 2.0 | 57.9 | 3.2 | 15.8 | 15.7 | 6.0 | 1.6 |
| an be. | Single Slag | 1.40 | 0 | 0.77 | 52 | -0.12 | Al | 3180 | Before Mn and Si | | 53.0 | 0.53 | 34.4 | 7.6 | 3.2 | 1.9 |
| Single Slag | 0.07 | 1.10 | 0 | 0.35 | 30 | -0.20 | 0 | 3300 | Ladle | No S | Samples | 3.0 | 15.0 | 1.61 | 3.3 | 6.1 |
| Single Slag | 0.19 | 0.74 | * | 0.80 | 82 | +0.10 | Al | 3250 | Before Mn | 1/2 | 8.8 | | 10.9 | 15.0 | 4.7 | 2.8 |
| Perular Single Slag | 0.10 | 3.05 | 5 1.95 | 1.20 | 38 | +0.29 | 0 | 3300 | Ladle Before Mn Tap | 3.5 | 54.2 53.8 | | 12.3 | 20.8 14.6 22.3 | 4 - 4 - | 3.4 |
| Rerular Single Slag | 0.14 | 3.15 | 5 2.36 | 1.60 | 49 | +0.26 | 0 | 3200 | Eefore Mn | 3.7 | 52.7 | | 22.6 | 24.2 24.2 | 5.00 | 2.5 |
| Perular Single Slag | 0.12 | 60 | .50 | 1.43 | 40 | +0.27 | 0 | 3300 | Ladle Before Mn Tap | 5.0 | 50.2 | 200.0 | 22.0 | 24.1 | 5.0 4.0 4.0 | 3.3 |
| 2 Slag (1 Dol-2 Sand) | 0.11 od) | 1 | 05: | 1.42 | 71 | +0.14 | 0 | 3180 | Ladie Before Mn Tap | 1.0 | 48.7 65.6 61.5 | | 13.4 | 34.8 6.3 11.3 | 7.0 | 4.8.1. |
| 2 Slag (2 Dol-3 Sand) | nd) 0.12 | 1 | .80 | 1.19 | 62 | +0.18 | 0 | 3180 | Ladle Before Block Before Mn | 1.0 | 56.8 58.6 62.0 | 1.9 | 12.7 | 10.4 | 9.5 | 7.0 |
| 3 Slag | 0.14 O.14 | - | .50 1.27 | 1.03 | 64 | +0.11 | 0 | 3200 | Before Mn | 1.5 | 65.8 | | 11.2 | 3.9 | 10.9 | 7.5 |
| Single Slag + Mr.0 | 0.13 | - | .50 1.43 | 1.06 | 65 | +0.12 | Al | 3200 | Ore Down Before Mn | 6.0 | 49.2 | | 22.7 | 14.8 | . v. v. v. | 0 8 8 9 6 70 4 6 |
| 4 | 2 Slag | 1. | 50 1.20 | 1.03 | 20 | +0.09 | Al | 3200 | Before Mn | 1.7 | 51.0 | | 12.1 | 14.1 | 3.4.7 | 2.0 |
| 710 | Jul & Candy | 1. | .50 1.12 | 1.03 | 64 | +0.05 | IA | 3200 | Eefore Mn | 2.2 | 60.0 | | 14.1 | 12.1 | 7.7 | 5.0 |
| 0,0 | 2 Slag 0.13 | 1 | .50 1.24 | 0.88 | 54 | +0.02 | 0 | 3350 | Before Mn | 4.5 | 55.5 | | 18.7 | 18.3 | 0.4. €.8. | 25.0 |
| 2 Slag (4 Del. 7 Sard | the + | 1. | .50 11.6 | 96.0 | 03 | +0.01 | Al | 3250 | Before Mn Ladle | 3.0 | 54.5 54.5 | | 12.3 | 15.8 | 7.9 | 5.0 |
| O. 3 | 2 Slrp (2 Mr O, 3 Sand) | 2 | 2.30 | 1.55 | 42 | + 0.12 | | 3180 | Fefore Fleck Before Mn | 3.2 | 55.4 | 0.0 | 18.0 | 19.6 | 3.8 | 2.3 |
| 2 Slag (1 Ecl, 2 Sard) | erd) 0.12 | - | .50 1.31 | 1.01 | 62 | +0.06 | Al | 3200 | Before Mn Ladle | 1.5 | 65.8 50.7 | 4.1 | 12.8 | 5.9 | 3.7 | 2.9 |
| r cn | 2 Slep (5 Line-7 Serd) | 1 | .50 1.15 | 5 1.01 | 1 62 | + 0.09 | 0 | 3200 | Fefere Mn Ladle | 1.7 | 63.4 | 0.1 | 12.5 | 6.2 | 15.1 | 1.6 |
| Cher ch alter | citer Mn C.12 | 1 1 | 05 | 1.20 | 0 64 | + .19 | 0 | 3230 | Pefere Mn | 1.5 | 62.1 | | 14.1 | | 8.6 | 6.0 |
| 1.7 6.1 | 2 Slrg (3 Fel-2 Sord) | 0 1 | .50 1.09 | 63.0 6 | 95 6 | + .20 | 0 | 3350 | Fefere Mn | 4.0 | 58.6 | 1.9 | 11.0 | 7.6 | 13.2 | 7.9 |
| 4 . | 2 Slag | 4 1 | .50 1.18 | 8 0.74 | 45 | 10 | 0 | 3200 | Befre Mn | 4.5 | 54.6 | | 33.2 | | 111 | 0.0 |

TABLE 1 (CONT.) -ACID ELECTRIC HEATS

| Exp. | Heat | Type | | % Mn | % Mn | % Mn | | % Mn | 15 % | albe I | Tont | | Slag | | | Slag Co | mposition | | |
|------|-------|--------------------|------------------------------|----------|-------|------|-------|----------|----------|------------|---------|-----------------------------|-----------|-------|-------|---------|-----------|----------|--------------|
| No. | No. | Steel | Practice | Residual | Added | Tap | Final | Recovery | Recovery | Deoxidizer | Temp.,F | Sampled | Vis., in. | SiOs | Al203 | Fe3 | OuM Cea | CaO | MgO |
| 23 | 35056 | 0.25% C 0.85 Mn | 2 Slag (2 M Scale-3 Sand) | | 1.50 | 1.07 | 0.86 | 53 | + .05 | 0 | 3200 | Before Mn | 1.5 | 58.94 | 2.0 | 30.6 | 6.5 | 1.8 | 0.5 |
| 24 | 55180 | 0.25% C 0.65 Mn | 2 Slag (1 Dcl, 2 Sard) | 60.0 | 0.00 | 0.76 | 0.70 | 71 | + .10 | Al | 3250 | Before Mn Tap | 1.5 | 65.4 | 2.4 | 13.9 | 7.3 | 7.3 | 4.8 |
| 25 | 36160 | 0.25% C | 2 Slag | | 0.85 | | 99.0 | 89 | + | 0 | 3300 | Ladle Before Block | 3.2 | 56.8 | 10.7 | 13.8 | 7.6 | 12.2 | 4.00 6.00 |
| 26 | 35040 | 0.25% C 0.65 Mn | 2 Slag (3 MnO, 5 Sand) | | 0.82 | | 0.63 | 29 | 0 | Al | 3200 | Ladle Before Mn Ladle | 2.0 | 58.8 | 0.4 | 15.5 | 18.5 | 20 es es | 2.1 |

* Recovery based on total Mn, residual plus addition. Where residual analyses not indicated 0.12% Mn assumed on heats.

** Viscosity determined with Herty Viscosimeter with 1/4 in. channel.

*** Total inon expressed as FeO = "FeO", (FeO) + (O) Fe2O),

** Total inon expressed as FeO = "FeO", (FeO) + (O) Fe2O),

*** Total inon expressed as FeO = "FeO", (FeO) + (O) FeO) + (O) FeO) + (O) FeO + (O)

TABLE 2-BASIC ELECTRIC HEATS

| | | | | | | | | | TVD | TE 5-1 | A ABLE 4-DASIC LIEUTRIC HEAL | RIC LIEVIS | | | | | | | | | | |
|------|-------|--------------|-------------------------|---------|---------|-------|----------|----------|---------|----------|------------------------------|----------------------|------------------|--------------------------------|------|------|-------------------|-----------|------|------|------|------|
| Fren | | Type | Furnaca | 0% Mn | 0% Mn | | - | 5 | Ladle | Ton | , E | Sal | | | X | Sla | Slag Composition, | sition, 9 | 200 | | | |
| No. | No. | Steel | Practice Residual Added | Residua | 1 Added | Final | ery * | Recovery | dizer T | Temp., F | Sam | ple Viscosity, in. S | SiO ₂ | Al ₂ O ₃ | Fe0 | MnO | CaO | MgO | CaFg | CaS | CaC2 | P206 |
| I | 56818 | 56818 0.25 C | Single Slag 0.21 | 0.21 | 0.75 | 0.84 | 88 | -0.11 | | 3300 | Ore Down | 7 | 21.0 | 8.0 | 18.7 | 6.4 | 27.7 | 5.9 | 1.1 | 0.10 | 0 | 1.3 |
| | | 0.75 Mn | | | | | | | | | Ecfore Mn | 9 | 48.1 | 5.7 | 6.9 | 5.5 | 27.9 | 5.2 | 0.7 | 0.14 | 0 | 0.7 |
| | | | | | | | | | | | Ladle | | 44.2 | 3.7 | 2.0 | 6.3 | 19.7 | 12.8 | 1.9 | 0.15 | 0 | 0.3 |
| II | 56840 | 0.25 C | Single Slag 0.19 | 0.19 | 0.70 | 0.72 | 81 | -0.07 | A1 | 3250 | Before Si | 1 | 22.2 | 7.00 | 8.4 | 3.3 | 31.3 | 16.1 | 1.0 | 0.03 | 0 | 0.14 |
| | | 0.75 Mn | | | | | | | | | Before Mn | | 32.9 | 8.4 | 4.0 | 3.2 | 26.8 | 22.8 | 1.5 | 0.01 | 0 | 90.0 |
| | | | | | | | | | | | Ladle | | 35.5 | 6.2 | 5.6 | 5.6 | 20.4 | 15.5 | 1.8 | 0.03 | 0 | 0.03 |
| H | 56816 | 0.25 C | Double Slag 0.18 | 0.18 | 0.70 | 0.84 | 96 | -0.10 | | 3220 | Oxide Slag | _ | 22.5 | 3.1 | 36.4 | 6.2 | 26.0 | 4.8 | 0.5 | 0.33 | 0 | 0.28 |
| | | 0.75 Mn | | | | | | | | | 2nd Slag | | 37.9 | 5.3 | 0.7 | 1.0 | 39.3 | 13.7 | 1.8 | 0.52 | 0.13 | 0.16 |
| | | | | | | | | | | | Before Mn | | 42.6 | 8.4 | 8.0 | 1.0 | 36.5 | 12.3 | 3.0 | 0.40 | 0.27 | 0.15 |
| | | | | | | | | | | | Ladle | | 48.5 | 2.9 | 1.2 | 4.2 | 27.8 | 16.7 | 6.0 | 0.39 | 0.07 | 0.15 |
| IV | 56842 | 0.25 C | Single Slag 0.20 | 0.20 | 1.30 | 1.19 | 08 | -0.05 | A1 | 3350 | Before Si | 00 | 30.5 | 7.6 | 8.0 | 2.6 | 15.1 | 28.0 | 6.0 | 0.03 | 0 | 0.05 |
| | | 1.20 Mn | | | | | | | | | Before Mn | | 32.7 | 5.00 | 3.0 | 4.2 | 16.4 | 24.2 | 6.0 | 0.04 | 0 | 0.02 |
| | | | | | | | | | | | Ladle | | 36.1 | 00 | 2.8 | 10.4 | 11.2 | 16.5 | 1.0 | 0.05 | 0 | 0.02 |

*Recovery based on total Mn, residual plus addition.

** Viscosity determined with Herty viscosimeter with 1/4 in. channel.

*** Total iron expressed as FeO = "FeO", (FeO) + 0.9 (Fe₂O₃).

† Optical readings on stream using L & N disappearing filament, potenticmeter type of pyrometer with 0.4 emissivity correction, standardized with a thermocouple at 2600 F.

TABLE 3—COMPARISON OF OXYGEN AND ORE PRACTICE (All 1040 Steels From Same Furnace and Same Charge)¹

| 1 | - | 61.19 | , | December | Motol | T | | Metal | | Slan | | | Slag Co | Slag Composition, %5 | n, %s | |
|--------|-------------------|-------|---------------|---------------------------|-------------|----------------|------|-------|------|------------|------|--------------------------------|---------|----------------------|-------|-----|
| No. | Agent | Sag. | Summary | Operation | Temp.F3 | Sample | Si | Mn | О | Vis., in.4 | SiO2 | Al ₂ O ₃ | FeO | MnO | CaO | MgO |
| | | | | HEA | HEATS USING | G OX YGEN | | | | | | | | | | |
| (1) | Oxygen | 3.2% | 0.25 residual | Melt with 1/2% limestone | 2900 | Melted | 0.18 | 0.58 | 0.49 | 100 | 45.6 | 3.3 | 7.6 | 18.3 | 21.9 | 1.2 |
| 37383 | 100 cu ft per ton | | 0.85 added | Oxygen injected | 2890 | After 02 | 90.0 | 0.25 | 0.33 | 2 | 48.0 | 2.4 | 10.7 | 21.8 | 10.5 | 0.7 |
| | | | 0.84 final | | 3050 | Block | 0.11 | 0.25 | 0.38 | 21/2 | 55.0 | 2.2 | 12.1 | 21.2 | 6.8 | 9.0 |
| | | | Recovery 76% | Block 0.40 Mn and 0.20 Si | 3160 | Final Addition | 0.70 | 0.52 | 0.34 | | 58.0 | 2.1 | 8.7 | 21.7 | 00. | 9.0 |
| | | | | Balance 0.45 Mn | 3230 | Tap | 0.37 | 96.0 | 0.41 | | 65.7 | 1.6 | 7.2 | 19.0 | 6.2 | 0.4 |
| | | | | + 0.10% Al in Ladle | | Ladle | 0.46 | 0.84 | 0.40 | | 58.6 | 5.7 | 5.5 | 23.8 | 0.0 | 0.5 |
| (2) | Oxygen | 2.5% | 0.21 residual | Melt with 1/2% limestone | 2900 | Melted | 0.22 | 0.55 | 0.48 | 21/2 | 51.5 | 3.0 | 9.8 | 16.8 | 19.3 | 6.0 |
| 37.386 | 116 cu ft per ton | 2 | 0.80 Added | Oxvgen injected | 2940 | After 0, | 0.00 | 0.21 | 0.28 | 51% | 48.7 | 1.9 | 20.3 | 21.4 | 7.9 | 0.5 |
| | | | 0.81 final | Recarburize and Heat | 3100 | Block | 0.12 | 0.21 | 0.34 | 2 | 58.6 | 1.8 | 11.8 | 19.8 | 7.6 | 0.5 |
| | | | Recovery 80% | Block 0.40 Mn, 0.20 Si | 3190 | Final Addition | 0.38 | 0.50 | 0.36 | | 64.2 | 1.9 | 7.00 | 18.6 | 7.0 | 0.4 |
| | | | | Balance 0.40 Mn | 3220 | Tap | 0.43 | 0.92 | 0.42 | | 65.7 | 1.7 | 7.7 | 17.6 | 6.4 | 0.4 |
| | | | | + 0.10 Al in Ladle | | Ladle | 0.46 | 0.81 | 0.47 | | 8.19 | 5.1 | 5.7 | 21.0 | 00. | 0.5 |
| (3) | | 2.2% | 0.16 residual | Melt with 1/2% limestone | 3050 | Melted | 0.22 | 0.43 | 0.46 | | 52.1 | 3.0 | 10.8 | 17.8 | 16.5 | 8.0 |
| 37380 | 188 cu ft per ton | 2 | 0.90 Added | Oxygen injected | 3150 | After 02 | 0.05 | 0.15 | 90.0 | 41/2 | 55.5 | 3.0 | 14.5 | 17.9 | 8.8 | 0.4 |
| | | | 0.84 final | Recarburize | 3100 | Block | 0.10 | 0.16 | 0.28 | 11/2 | 8.00 | 3.1 | 14.1 | 15.2 | 9.9 | 0.3 |
| | | | Recovery 79% | Block 0.40 Mn, 0.20 Si | 3180 | Final Addition | 0.30 | 0.44 | 0.35 | 2 | 4 | 3ad Sami | ple | | | |
| | | | | Balance 0.50 Mn | 3230 | Tap | 0.34 | 0.89 | 0.39 | | 67.0 | 3,3 | .94 | 17.7 | 6.5 | 0.3 |
| | | | | + 0.10% Al in Ladle | | Ladle | 0.40 | 0.84 | 0.37 | | 58.9 | 0.9 | 7.0 | 20.9 | 6.9 | 0.4 |
| (4) | | 4.90% | 0.17 residual | Melt with 1/2% limestone | 2850 | Melted | 0.13 | 0.39 | 0.42 | 31/2 | 53.7 | 1.8 | 19.4 | 13.3 | 11.0 | 9.0 |
| 37393 | 150 cu ft per ton | 2 | 0.80 Added | Oxvgen injected | 2870 | After 02 | 0.03 | 0.13 | 0.23 | 10 | 48.8 | 1.6 | 26.3 | 15.7 | 7.2 | 0.4 |
| | | | 0.65 final | Recarburize and Heat | 3000 | Block | 0.10 | 0.17 | 0.28 | 31/2 | 60.2 | 1.5 | 14.3 | 15.7 | 7.3 | 0.4 |
| | | | Recovery 67% | | 3120 | Final Addition | 0.19 | 0.40 | 0.34 | 2 | 9.09 | 1.7 | 12.0 | 18.4 | 7.4 | 0.5 |
| | | | | Balance 0.40 Mn, 0.05 Si | 3250 | Tap | 0.70 | 0.79 | 0.41 | | 64.5 | 1.5 | 6.7 | 17.0 | 6.4 | 0.4 |
| + | | | | + 0.10% Al in Ladle | | Ladle | 0.29 | 0.05 | 0.40 | | 60.2 | 4.5 | 8.9 | 18.7 | 6.9 | 9.0 |
| | | | | | HEATS US | SING ORE | | | | | | | | | | |
| (5) | | 5.1% | 0.17 residual | 0.7% limestone 41/2% ore | 2900 | Melted | 0.05 | 0.18 | 0.30 | 111 | 41.0 | | 33.3 | 11.4 | 10.2 | 1.2 |
| 37381 | 41/2% | | 1.00 Added | Vigorous boil | 3070 | Boil | 0.02 | 0.17 | 0.22 | 3 | 50.8 | | 23.1 | 13.5 | 0.6 | 1.0 |
| | | | 0.78 final | Recarburize and Heat | 3150 | Block | 80.0 | 0.17 | 0.70 | 21/2 | 55.6 | | 17.8 | 13.7 | 9.6 | 8.0 |
| | | | Recovery 67% | Block 0.40 Mn, 0.20 Si | 3200 | Final Addition | 0.28 | 0.35 | 0.36 | - | 62.5 | | 11.8 | 15.1 | 7.7 | 8.0 |
| | | | | Balance 0.60 Mn, 0.05 Si. | 3220 | Tap | 0.38 | 0.89 | 0.43 | | 0.00 | | 9.6 | 14.5 | 7.4 | 0.0 |
| *** | | | | + 0.10% Al in Ladle | 0000 | Ladle | 0.43 | 0.78 | 0.43 | | 60.5 | | 5.00 | 18.9 | 7.1 | 0.7 |
| (9) | Ore | 4.1% | 0.16 residual | 0.7% limestone 4½% ore | 2050 | Melted | 40.0 | 0.20 | 0.39 | _ | 38.3 | | 55.2 | 14.7 | 5.6 | 0.1 |
| 3/387 | | | 1.00 Added | Vigorous boll | 2120 | Boll | 0.00 | 0.13 | 47.0 | 2117 | 4.14 | | 6.27 | 0.71 | 9.0 | 1.1 |
| | | | Pecovery 670% | Rlock 0 40 Mn. 0.20 Si | 3200 | Final Addition | 0.30 | 0.10 | 0.38 | 2/2 | 57.0 | | 10.0 | 18.2 | 10.0 | 1.1 |
| | | | 0/10 (121020) | Balance 0.60 Mn, 0.05 Si | 3250 | Tap | 0.38 | 0.97 | 0.45 | | 56.0 | 2.2 | 9.2 | 21.5 | 6.6 | 11 |
| | | | | + 0.10% Al in Ladle | | Ladle | 0.45 | 0.77 | 0.43 | | 54.1 | | 7.2 | 24.5 | 9.8 | 1.0 |
| | | | | | | | | | | , | | | | | | |

¹ Scrap shells 0.20% Si, 0.70% Mn, 0.50% C.

² Ratio of weight of slag to weight of metal charge.

³ Optical temperature reading in spoon and tap stream.

1

METAL AND SLAG CHANGES-NORMAL SINGLE SLAG HEATS

| | Metal | Composi | tion, % | 5 | Slag C | ompo | sition, | % | |
|--------------------|--------|---------|---------|---------|--------------------------------|------|---------|-----|-----|
| | Mn | Si | Al | SiO_2 | Al ₂ O ₃ | FeO | MnO | CaO | MgO |
| Heat 1 Before | | | | | | | | | |
| Additions | 0.15 | 0.04 | 0 | 53.9 | 2.3 | 18.8 | 17.5 | 3.3 | 2.3 |
| Additions made* | (0.95) | (0.50) | 0 | | | | | | |
| Final ladle sample | 0.62 | 0.40 | 0 | 58.0 | 1.8 | 10.3 | 22.6 | 3.5 | 2.8 |
| Heat 2 Before | | | | | | | | | |
| Additions | 0.12 | 0.05 | 0 | 57.9 | 3.2 | 15.8 | 15.7 | 4.0 | 1.6 |
| Additions made* | (1.15) | (0.50) | (0.12) | | | | | | |
| Final ladle sample | 0.77 | 0.45 | 0.05 | 59.1 | 5.7 | 8.4 | 18.7 | 3.6 | 1.8 |

^{*} Figure in parenthesis indicates theoretical additions to bath. Other figures are actual analyses.

With the residual included, the Mn recovery obtained was 59 per cent. Within the interval producing the Mn loss, the MnO content of the slag increased from 17.5 to 22.6 per cent. This additional MnO was presumably produced by the generally accepted deoxidation reaction:

Mn (m) + FeO (m&s) = MnO (s) + Fe (m)In the same period the slag SiO_2 increased from 53.9 to 58.0 per cent, roughly accounting for the Si lost from the metal by a similar reaction:

 $Si(m) + FeO(m\&s) = SiO_2(s) + Fe(m)$ The decrease of slag FeO* from 18.8 to 10.3 per cent indicated that the iron oxide that diffused from the slag supplied the necessary reactant for the oxidation of these quantities of Mn and Si. But the reason for the FeO stopping at 10.3 per cent requires some further explanation.

An attempt was made to account quantitatively for the Mn lost on this particular heat. After pouring this 6000-lb heat the slag was collected and the weight determined as 330 lb or 5.5 per cent* of the metal charge weight. The 1.03 per cent Mn lost from the charge and the addition could have produced 79.9 lb MnO. Actually 93 per cent of this amount or 74.6 lb of MnO was detected in the final slag.

TOTAL MN LOST FROM METAL VS MNO IN FINAL SLAG

| Tot | al Mn lost from charge and addition = 1.03% Mn |
|------|---|
| 1.03 | % × 6000 lb == 61.8 lb Mn lost |
| 61.8 | 3 lb Mn \times 71/55 \equiv 79.9 lb MnO possible from total Mn lost |
| Fina | al weight = 330 lb/6000 lb charge |
| Fina | al slab MnO content = 22.6% |
| 330 | lb $	imes$ 22.6% = 74.6 lb MnO actually present in final slag collected |

The balance could easily have been within the errors inherent in sampling and analyzing and in collecting and weighing the slag. On the other hand there is the possibility that the balance might have escaped the system by volatilization or some other reaction.

Heat No. 2 experienced similar losses of silicon and manganese from the metal, and similar increases of MnO and SiO₂ in the slag at the expense of the slag FeO. On this heat, the aluminum was added in the tapping ladle where the slag in contact with the steel

* Slag FeO reported as FeO plus Fe2O3 corrected to FeO.

caused oxidation of 60 per cent of the aluminum added. The oxidized aluminum increased the ${\rm Al_2O_3}$ of the slag from 3.2 to 5.7 per cent. The reaction with the aluminum consumed some FeO in addition to that used in the Si and Mn reaction for a total decrease of slag FeO from 15.8 to 8.4 per cent.

Heat No. 3 was deliberately made to finish in a more oxidized condition than normal, as evidenced by the 34.4 per cent FeO in the slag. A 1.40 per cent Mn and 0.60 per cent Si addition was made 2 min before the heat was tapped at 3250 F.* The 1.40 per cent addition to the 0.08 per cent residual Mn produced a 0.77 per cent Mn final. Slag FeO was decreased from 34.4 to 15.6 per cent, slag MnO was increased from 7.6 to 15.1 per cent and slag SiO₂ increased from 53.0 to 61.1 per cent. Loss of the elements and changes in the slag were similar to the previous heats but greater than normal because of the higher FeO of the slagmetal system.

METAL AND SLAG CHANGES-UNUSUALLY OXIDIZED SINGLE SLAG HEATS

| | Metal | Metal Composition, % | | | | Slag Composition, % | | | | | | |
|---------------|--------|----------------------|--------|------------------|-------------------|---------------------|-------|-------|-----|--|--|--|
| | Mn | Si | Al | SiO ₂ | Al ₂ O | FeO | MnO (| CaO 3 | MgO | | | |
| Heat 3 Before | | | | | | | | | | | | |
| Addition | 0.08 | 0.02 | 0 | 53.0 | 0.5 | 34.4 | 7.6 | 3.2 | 1.9 | | | |
| Addition | (1.40) | (0.60) | (0.12) | | | | | | | | | |
| Ladle Sample | 0.77 | 0.50 | 0.04 | 61.1 | -3.0 | 15.6 | 15.1 | 3.3 | 2.0 | | | |
| Heat 4 Before | | | | | | | | | | | | |
| Addition | 0.07 | 0.02 | 0 | Not | Samp | led | | | | | | |
| Addition | (1.10) | (0.60) | 0 | | | | | | | | | |
| Ladle Sample | 0.35 | 0.40 | 0 | | | | | | | | | |

Although slag samples were not obtained, a still greater Mn loss was obtained on Heat 4, a 0.05 per cent C, 0.40 per cent Mn steel. Extreme oxidation was necessary to obtain the low carbon. An addition of 1.10 per cent Mn to a residual of 0.07 per cent yielded only 0.35 per cent final Mn, a loss of 70 per cent of the total Mn.

METAL AND SLAG CHANGES—UNUSUALLY LOW FEO SINGLE SLAG HEAT

| | Metal Composition, % | | | Slag Composition, % | | | | | |
|---------------|----------------------|--------|--------|---------------------|-------------------|------|------|-------|-----|
| | Mn | Si | Al | SiO ₂ | Al ₂ O | FeO | MnO | CaO I | MgO |
| Heat 5 Before | | | | | | | | | |
| Addition | 0.19 | 0.05 | 0 | 64.8 | 4.0 | 10.9 | 15.0 | 4.7 | 2.8 |
| Addition | (0.74) | (0.25) | (0.12) | | | | | | |
| Ladle Sample | 0.80 | 0.40 | 0.08 | 60.8 | 4.9 | 8.5 | 20.8 | 3.4 | 2.4 |

At the other extreme, Heat 5 represents the lowest FeO single slag acid heat from which complete slag data was obtained. This heat of 0.45 per cent C, 0.75 per cent Mn steel, had been melted from a clean charge in a newly lined furnace with a lower than average quantity of slag, a weight ratio of slag to metal of 3.5 per cent. Carbon was oxidized from 0.40 per cent down to 0.22 per cent C leaving a high residual Mn of 0.19 per cent. After recarburization and a 0.20 per cent Si block, the slag had the appearance of a very low FeO content familiar to experienced acid melters. This slag analyzed only 10.9 per cent total FeO. The Mn addition was cut to 0.74 per cent, which added to the 0.19 per cent residual, yielded a final of

^{*•} Throughout the paper slag quantity will be expressed as per cent of metal charge weight.

[•] All temperatures from optical readings with 0.4 emissivity correction using disappearing filament, potentiometer type pyrometer standardized with a thermocouple.

0.80 per cent Mn. Ignoring the residual Mn, recovery based on the addition alone was 108 per cent. However, with the 0.19 per cent residual properly included,

the true recovery was 82 per cent.*

During the reaction slag FeO was decreased only 2.4 per cent while MnO was increased 5.8 per cent by the reaction. Contrary to the other four heats, slag SiO₂ decreased rather than increased. If the reaction were simply an oxidation of Mn by the slag FeO, then based on the 2.4 per cent FeO consumed from the slag, both the increase in slag MnO and the Mn lost from the steel should have been only half that experienced.

Quantitative calculations on this heat below indicate that it is possible for the MnO in the final slag to account for 88 per cent of the total Mn lost from the metal on this heat.

MN LOST FROM METAL VS MNO IN FINAL SLAG

Charge Addition Final Lost $0.70\% \times 0.74\% - 0.80\% = 0.64\%$ Mn 0.64% Mn \times 4000-lb Charge = 25.6 lb Mn lost 25.6 lb Mn \times 71/55 = 33 lb MnO possible from the total Mn lost 4000-lb Metal Charge Final slag weight = 140 lb = 3.5% Final composition = 20.8% MnO 140 lb \times 20.8% = 29.2 lb MnO actually present in final slag collected

These five heats illustrate the variations in Mn recovery possible in acid steel making and emphasize the importance of understanding the factors that control the reactions involved. With the exception of Heat 5, Mn loss from these steels seems to be chiefly a simple oxidation of the Mn along with the silicon by the FeO in solution in the steel plus the FeO that migrates from the slag into the steel as the reaction proceeds. The higher the slag FeO, the greater the extent of this reaction and the greater the loss of both Mn and Si.

However, Heat 5 had been deoxidized previous to the Mn addition with Si, a stronger deoxidizer than Mn. Silicon was reduced from the silica of the slag into the metal while Mn was oxidized in the other direction. Some explanation beyond simple oxidation seems necessary to explain how within the same system Mn can be oxidized to its oxide while the oxide of silicon is being reduced to the element.

Higher Mn Stee!-Single Slag Practice

If all the Mn loss were due simply to oxidation by excess FeO, then after consumption of the necessary FeO, no further loss should occur from higher Mn additions; and better percentage recovery should be obtained on higher Mn steels. Contrary to this supposition, 1.45 per cent Mn steels tend to experience proportionately higher losses than 0.75 per cent Mn steel finished with the same degree of oxidation. The reaction appears sensitive to more variables and does not follow any fluidity relationship with any practical degree of accuracy. The three following heats (Heats 6, 7, and 8) illustrate the unusual losses that instigated

this study. Correspondingly large MnO increases in the slag seem to account for the loss.

1.45 PER CENT MN STEELS-SINGLE SLAG

| | Mn,% | Vis. | | Al ₂ O ₃ | FeO | MnO | CaO | MgO |
|------------------------|--------|----------|------|--------------------------------|------|------|-----|-----|
| Heat 6 Before Addition | 0.10 | 3.5 | 54.2 | ? | 19.5 | 14.6 | 5.1 | 3.4 |
| Addition | (3.05) | 38% | | | | | | |
| Before Tap 3300° | 1.95 | Recovery | 53.8 | ? | 12.3 | 22.3 | 4.4 | 3.1 |
| Ladle Sample | 1.20 | | 50.3 | ? | 6.8 | 32.5 | 3.8 | 2.6 |
| Heat 7 Before Addition | 0.14 | 3.7 | 52.7 | 0.4 | 22.6 | 16.1 | 6.0 | 5.5 |
| Addition | (3.15) | 49% | | | | | | |
| Before Tap 3200° | 2.36 | Recovery | 50.9 | 0.5 | 12.6 | 24.2 | 5.5 | 5.6 |
| Ladle Sample | 1.60 | | 48.8 | 0.5 | 8.3 | 32.0 | 5.0 | 5.7 |
| Heat 8 Before Addition | 0.12 | 5.0 | 50.2 | 2.2 | 22.0 | 17.3 | 4.1 | 3.3 |
| Addition | (3.50) | 40% | | | | | | |
| Before Tap 3300° | | Recovery | 51.7 | 2.5 | 14.9 | 24.1 | 4.0 | 3.0 |
| Ladle Sample | 1.43 | | 48.7 | 1.6 | 7.5 | 34.8 | 3.5 | 3.4 |

These are 1.45 per cent Mn, 0.30 per cent C steels with a 0.35 per cent desired Si, made by a regular single slag practice. To Heat 6 with a slag fluidity of 3.5 in. and a residual Mn of 0.10 per cent, an addition of 3.05 per cent Mn was made. After tapping 3 min later at 3300 F, a final Mn of only 1.20 per cent was obtained from the ladle sample. Including the residual, actually 1.95 per cent Mn was lost from the metal, a loss of 62 per cent of the total Mn. An increase in slag MnO from 14.6 per cent to 32.5 per cent MnO occurred from the Mn lost. The 12.7 per cent FeO given up by the slag was not nearly enough to supply the 17.9 per cent MnO increase. Instead of losing Si by oxidation, a silicon 0.29 per cent higher than added was obtained. The 4.2 per cent SiO2 decrease in the slag indicates the additional Si was obtained by reduction of the SiO2 of the slag. The usual conditions associated with silica reduction by carbon, such as viscous low FeO slag, white smoke, slag boil, carbon drop, etc. were definitely absent on these heats. The evidence seems to indicate that the silicon was reduced by Mn according to the following reaction:

$$2 \text{ Mn (m)} + \text{SiO}_2 = 2 \text{ MnO (s)} + \text{Si (m)}$$

Although this forward reaction is contrary to the relative deoxidizing strengths of the two elements, as generally conceived, such a reaction would be favored by the mass action laws because of the excess of reactants on the left and the deficiency of products on the right. The effect of Mn on silica reduction has been suggested in the literature for the purpose of accounting for silicon obtained from 12 per cent Mn steel in acid ladles, etc.⁸ No description has been found of the importance and the quantitative effect of this reaction on Mn recovery.

Quantitative calculations on Heat 6 (Table 1), are shown below. The weight of the final slag collected from this 4060-lb heat was 350 lb or 8.8 per cent of the charge weight. However the 0.60 per cent Mn oxidized from the charge could not have produced a 14.6 per cent concentration in more than a 5.5 per cent slag quantity. It is necessary to assume considerable increases in slag quantity during the heat and especially after the addition of the Mn. The additional MnO necessary to increase the concentration from 14.6 to 32.5 per cent along with the increase in total slag quantity from the assumed 220 lb to the weighed

Unless otherwise indicated, recovery calculations will include residual Mn.

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350 lb accounts for 82.8 of the 101 lb possible from the Mn lost from the addition. The final slag MnO accounts for 86 per cent of the total Mn lost from the charge and the addition.

CALCULATIONS OF METAL AND SLAG CHANGES— HEAT 6, 4000-LB CHARGE

Mn lcst from Oxidation of Charge

From oxygen of charge 0.70 Mn - 0.10 \pm 0.60% Mn lost 0.60% \times 4000-lb charge \pm 24.0 lb Mn 24 lb Mn \times 71/55 \pm 31 lb MnO possible from Mn oxidized from charge

Assume 5.5% or 220 lb slag before Mn at 14.6% MnO 220 \times 14.6% MnO = 31.2 lb MnO present in slag from oxidation of charge

Mn lost from Mn addition

| Addition | Residual | Final | Lost | |
|-------------|---------------|------------|------------|------------------|
| 3.05% Mn | + 0.10% - | 1.20% = | = 1.95% N | fn |
| 1.95% Mn | × 4000 lb d | harge = | 78 lb Mi | 1 |
| 78 Mn→10 | l lb MnO pe | ossible fr | om Mn los | st from addition |
| 31.2 lb Mn | O in slag be | fore add | ition | |
| 350 lb × 3 | 32.5% MnO | = 114 1 | MnO in | final slag |
| 114 lb - 3 | 1.2 = 82.8 lb | . MnO a | ctually ad | ded to slag |

Total Mn lost vs MnO in final slag

| 2.55% | Mn total loss | * |
|---------|-------------------------|---------------------------------|
| 2.55% | \times 4000 = 1.02 lb | Mn |
| 1.02 lb | Mn 132 lb MnO | possible from total Mn lost |
| | final slag at 32.5% | |
| 850 V | 89 5 - 114 lb Mn | O actually present in final ala |

The other two heats illustrate the erratic recovery experienced with three apparently similar slags. Recoveries of Mn from 38 to 49 per cent were experienced. This variation was sufficient to produce final Mn contents 1.20 per cent, 1.43 per cent, and 1.60 per cent. On this specification Mn content was preferred between 1.35 and 1.55 per cent for hardenability control. Actually, the greatest Mn loss resulted from the heat with the lowest slag FeO and the lowest slag fluidity (Heat 6). Similar reductions of silicon into the metal were experienced on all three heats. Changes in slag composition followed the same line, a decrease in SiO₂ and FeO, and a 15 to 18 per cent increase in MnO. The similarity of all the final slags after the Mn reaction suggests some inclination toward an equilibrium composition in the final system.

Another observation of significance was the analysis of the samples taken only 2 min after the Mn addition and just before the tap. Both the metal and slag compositions indicated that only half or less of the re-

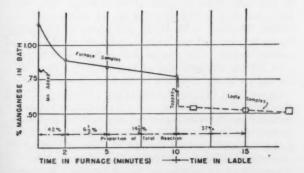


Fig. 1-Progress of Manganese Reaction with Time.

action had been completed in the furnace. The balance of the reaction apparently took place in tapping the steel from the furnace into the ladle as indicated by the further decrease of the Mn in the metal and the further increase of the MnO of the slag.

Diffusion Path and Reaction Rate

In order to determine the effect of time, several heats were held in the furnace longer than our usual 2 or 3 min after the Mn addition. Samples were taken at several intervals. Sample results on a typical heat follow. Progress of the Mn reaction with time is shown graphically in Fig. 1.

| Heat 35541 | Si, % | Mn, % |
|--|--------|--------|
| Residuals before additions | 0.05 | 0.15 |
| Additions to furnace | (0.45) | (1.00) |
| Total (theoretical) in metal | (0.50) | (1.15) |
| Furnace- 2 min after Mn | 0.43 | 0.89 |
| Furnace- 5 min after Mn | 0.41 | 0.85 |
| Furnace-10 min after Mn (Tapped from furnace) | 0.41 | 0.76 |
| [1st 10% (3 min) | 0.46 | 0.55 |
| Ladle samples { Middle (15 min) | 0.45 | 0.53 |
| Last 10% (30 min) | 0.45 | 0.50 |

Of the 62 points of Mn lost from the metal, 42 per cent of the loss occurred within the first 2 min, only 21 per cent within the additional 8 min in the furnace, and the last 37 per cent of the reaction took place while pouring from furnace to ladle. The amount of reaction that takes place in the absence of the arc and in the presence of the agitation encountered in tapping seems to provide further evidence that the principal Mn loss is caused by a reaction between metal and slag. The reaction curve seems consistent with interface types of reactions.

The direction and rate at which lump ferromanganese distributes itself into the bath was indicated by samples taken from various locations within a still furnace bath soon after adding the ferromanganese. The results on three heats in Fig. 2 show a higher Mn at the top of the bath than at the bottom 2 min after the additions with no agitation. The higher Si contents near the slag seem further evidence that the additional Si was reduced from the slag rather than from the hearth in this case.

Using smaller lumps of ferromanganese, equalization of the bath was much more rapid and lower Mn baths were found to become uniform in composition more rapidly than higher Mn steels as shown in Fig. 3.

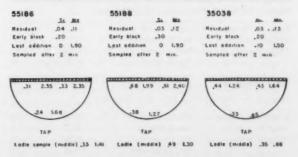
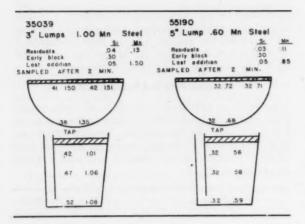


Fig. 2-Silicon and Manganese Distribution in Bath Sampled 2 Min after Addition of 5-in. Lump FeMn.



Stirring with a metal rod was found to equalize both compositions. Again samples from the last steel in the ladle ran lower than samples from the first steel, as shown in Fig. 1. Investigation of ladle uniformity of a number of such small acid heats has verified this general inclination for the Mn to drop 0.05 to 0.10 per cent during the 30 min pouring time. This drop is apparently caused by a continuation of the Mn reaction between the slag and the metal in contact with it in the ladle.

These results lead to the conclusions that lump ferromanganese dissolves readily, but does not diffuse evenly into the remote sections of the bath for several minutes without some artificial agitation. In the furnace the Mn consuming reaction proceeds rapidly the first 2 min, then with decreasing speed as the reaction becomes dependent on diffusion. Agitation resulting from tapping the heat from the furnace to ladle makes possible further advancement toward equilibrium representing 1/4 to 1/2 of the total reaction.

Effect of Arc and Ladle Additions

In order to determine the effect of the arc, two heats were finished with the power turned off before the Mn addition (shown below, and Heats 18 and 19 in Table 1). Although these heats had experimental slag compositions, they were almost identical in every detail with Heats 6 and 7 that were arced upon. As indicated below, apparently identical recovery was experienced.

HEATS WITH NO HEAT INPUT AFTER MN ADDITION

| Heat 18, No. 35043 (1 | Power off, 1.50% Mn add | led) Si | Mn |
|-----------------------|-------------------------|---------|------|
| Furnace Samples | No. 1 Deep | 0.31 | 1.05 |
| After 2 min | No. 2 Shallow | 0.36 | 1.35 |
| After 5 min | No. 1 Deep | 0.36 | 1.33 |
| | No. 2 Shallow | 0.38 | 1.30 |
| Final sample from | Middle of ladle | 0.31 | 1.01 |
| Heat 19, No. 35055 (I | Power off, 1.50% Mn add | led) | |
| Furnace Samples | No. 1 Deep | 0.41 | 1.12 |
| After 2 min | No. 2 Shallow | 0.42 | 1.19 |
| After 5 min | No. 1 Deep | 0.40 | 1.14 |
| | No. 2 Shallow | 0.41 | 1.16 |
| | Middle of ladle | 0.44 | 1.01 |

Fig. 3-Silicon and Manganese Distribution in Bath and Ladle. Bath Sampled 2 Min after Addition of FeMn. Ladle Samples: First 10%, Middle and Last 10%.

Withholding any highly reactive alloy until the metal has been tapped into the ladle is an obvious way to reduce alloy loss regardless of the reaction mechanism causing the loss. In order to determine quantitatively the improvement in Mn recovery from ladle additions, lower Mn steel had to be used because of the prohibitive temperature loss with larger additions. Six heats were tapped with all the FeMn thrown into the ladle. Analyses of ladle samples are shown below along with four similar heats with all the Mn added in the furnace. Recovery of Mn was improved from 66 to 71 per cent by ladle additions, when properly based on averages of the three ladle samples.

HEATS WITH MN ADDED IN FURNACE

| | % Mn | Mn | La | dle Sample | s* | Mn |
|-----|-------|------|------|------------|------|-----------|
| No. | Added | Тар | lst | Mid. | Last | Variation |
| A | 1.10 | 0.90 | 0.75 | 0.75 | 0.70 | 0.05 |
| В | 1.00 | 0.86 | 0.69 | 0.71 | 0.69 | 0.02 |
| C | 1.05 | 0.79 | 0.68 | 0.67 | 0.64 | 0.04 |
| D | 1.10 | 0.90 | 0.82 | 0.78 | 0.73 | 0.09 |

HEATS WITH MN ADDED IN LADLE

| No. | Mn, % Added | 1st | adle Sampl Mid. | es* Last | Mn Variation |
|------------|--|--------------|--------------------|---------------------|-------------------|
| U | 1.00 | 0.60 | 0.76 | 0.72 | 0.16 |
| V | 0.90 | 0.54 | 0.65 | 0.63 | 0.11 |
| W | 0.90 | 0.65 | 0.80 | 0.75 | 0.15 |
| X | 0.90 | 0.45 | 0.65 | 0.55 | 0.20 |
| Y | 0.90 | 0.44 | 0.70 | 0.70 | 0.26 |
| Z | 1.00 | 0.70 | 0.76 | 0.83 | 0.13 |
| | Compa | arison | | Furnace Addition | Ladle Addition |
| Avera | ige Mn vari | ation in la | dle | 0.05 | 0.17 |
| Avera n | nge Mn reco niddle ladle nge Mn reco | very based o | on regular | 65% | 77% |
| | f 3 ladle sa | | | 66% | 71% |
| | sample wit sample wi | | | | |

The most conspicuous observation is the lack of uniformity of Mn content throughout the ladle when all the Mn is added in the ladle. When the Mn is added in the furnace in the regular manner, the Mn in the ladle tends to drop about 0.05 per cent Mn from the first to the last because of continuing reaction. However, with all the Mn added in the ladle both the first and last samples were lower than the middle sample. The spread was as high as 0.26 per cent Mn on one heat and averaged 0.17 per cent Mn for the six heats. Low Mn in the first part of a ladleaddition heat can be accounted for by failure of the floating FeMn to dissolve and diffuse proportionately into the steel in the bottom of the ladle. The lower Mn in the last sample can be accounted for by a greater continuation of the Mn reaction in the ladle when the addition is delayed. This lack of uniformity could prove serious on small foundry size heats that are not reladled, especially when chemical and

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hardenability specifications are narrow and small castings are poured from shank ladles.

These results indicate that Mn recovery and reaction rate are not affected to any significant extent by the absence of the arc after the addition. Losses of Mn have been decreased but not eliminated by the ladle additions. The decreased loss seems accounted for by limited opportunity to approach equilibrium because of reduced time, temperature, and agitation. When all the reaction takes place in the ladle, chemical uniformity within the ladle is sacrificed.

Comparison With Basic Slag Reaction

Near the end of this study one of the furnaces was operated with a basic lining for a period, giving an opportunity to verify the assumed differences between acid and basic slag reactions. Since the furnace, source of heat, charges, temperature, and metal chemistry were almost identical on both acid and basic heats, any differences in recovery experienced may be definitely attributed to the differences in slag chemistry.

Heats I and II (in Table 2 and below) were melted with a single slag basic practice essentially identical to acid single slag practice. Heat I was boiled to 0.10 per cent C. The original slag was retained and "cleaned up" with recarburizing additions and further boiling for 20 min before blocking with 0.40 per cent Mn and 0.20 per cent Si. Five minutes later a final addition of 0.35 per cent Mn and 0.20 per cent Si was added. After 2 min the heat was tapped at 3300 F with 0.10 per cent aluminum added in the ladle. From the 0.75 per cent Mn added a final of 0.84 Mn was obtained. Based on the addition only, Mn recovery was 112 per cent. Properly including the 0.21 per cent residual, Mn recovery was 88 per cent. To obtain a 0.84 per cent Mn on an acid heat oxidized to this extent and tapped at this temperature, an addition of twice that amount of Mn would have been necessary. Heat II was run by a similar practice except for a silicon block instead of a simultaneous Mn and Si block. A Mn recovery of 81 per cent was obtained.

BASIC SINGLE SLAG HEATS

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| 3/ | letal C | etal Composition, % | | | | Slag Composition, % | | | |
|------------------------|---------|---------------------|--------|-------|--------------------------------|---------------------|-----|------|------|
| | Mn | Si | Al | SiO | Al ₂ O ₃ | FeO | MnO | CaO | MgO |
| | | I, | No. 5 | 6818 | | | | | |
| After Ore Down | 0.21 | 0.02 | 0 | 21.0 | 8.0 | 18.7 | 6.4 | 27.7 | 5.9 |
| Block additions | (0.40) | (0.20) |) | | | | | | |
| Before final additions | 0.57 | 0.11 | | 48.1 | 5.7 | 6.9 | 5.5 | 27.9 | 5.2 |
| Final additions | (0.35) | (0.20) | (0.10) | | | | | | |
| Ladle | 0.84 | 0.31 | 0.31 | 44.2 | 3.7 | 2.0 | 6.3 | 19.7 | 12.8 |
| | | II, | No. | 56840 | | | | | |
| Before Si Block | 0.21 | 0.03 | 0 | 22.2 | 7.8 | 8.4 | 3.3 | 31.3 | 19.1 |
| Block addition | | (0.30) | | | | | | | |
| Before Mn | 0.19 | 0.26 | | 32.9 | 4.8 | 4.0 | 3.2 | 26.8 | 22.8 |
| Final Addition | (0.70) | (0.25) | (0.10) | | | | | | |
| Ladle | 0.72 | 0.51 | 0.02 | 35.5 | 6.2 | 2.6 | 5.6 | 20.4 | 15.5 |

 $^{\bullet}$ CaF₂, CaS, CaC₂, P₂O₅, contents omitted here but included in Table 2.

The slag compositions of these heats easily explain the good recoveries obtained. Throughout both heats both slag FeO and MnO were half or less that experienced in acid slags at the same stage of the heat. Because of a lower distribution coefficient, basic slags contain much less FeO than acid slags in equilibrium with metal of the same oxide content. The lower slag FeO requires less consumption of Mn and Si and the slag MnO increase occurring after the Mn addition is generally less than half that experienced in acid practice.

In Heat I the slag weight was obviously increased rapidly by the MgO fused from the hearth and the SiO₂ that dripped from the roof. This dilution caused a progressive decrease in the other constituents as the heat progressed. In both acid and basic slags at such temperatures, allowances must be made for such last minute contributions to the slag from furnace refractories.

The complete double slag process generally associated with basic practice gave almost complete Mn recovery. On Heat III (Table 2 and below) the oxidizing slag was drained off and a second slag of lime, spar, carbon, and FeSi was added. The final "carbidic" slag analyzed less than 1 per cent total FeO and only 1 per cent MnO. With residual included, a Mn recovery of 96 per cent was obtained. Slag composition changes were relatively small after the Mn addition except for those caused by solution of refractories.

BASIC DOUBLE SLAG AND HIGH MN HEATS

| N | fetal Co | mposi | tion, 9 | 6 | Slag | Slag Composition, % | | | |
|-----------------------|----------|--------|---------|-------|-----------|---------------------|------|------|------|
| | Mn | Si | Al | SiOg | Al_2O_3 | FeO | MnO | CaO | MgO |
| | | III, | No. 3 | 56816 | | | | | |
| Ore down 0.09% C | 0.18 | 0.02 | 0 | 22.5 | 3.1 | 36.4 | 6.2 | 26.0 | 4.8 |
| New Slag, Rec., | | | | | | | | | |
| Block | (0.40) | (0.20) |) | | | | | | |
| Second slag 25 min | | | | 37.9 | 5.3 | 0.7 | 1.0 | 39.3 | 13.7 |
| Before final addition | | | | 42.6 | 4.8 | 0.8 | 1.0 | 36.5 | 12.3 |
| Final additions | (0.30) | (0.20) | (0.10) | | | | | | |
| Ladle | 0.84 | 0.34 | 0.05 | 48.5 | 2.9 | 1.2 | 4.2 | 27.8 | 16.7 |
| | | IV, | No. | 6842 | | | | | |
| Before Si block | 0.20 | 0.03 | 0 | 30.5 | 7.6 | 8.0 | 7.6 | 15.1 | 28.0 |
| Block addition | | (0.48) | 1 | | | | | | |
| Before Mn | | | | 32.7 | 5.8 | 3.0 | 4.2 | 16.4 | 24.2 |
| Final Addition | (1.30) | (0) | (0.10) | | | | | | |
| Ladle | 1.19 | 0.44 | 0.03 | 36.1 | 8.1 | 2.8 | 10.4 | 11.2 | 16.5 |

CaC₂, CaF₂, CaS, P₂O₅ contents omitted here, but included in Table 2.

Heat IV is a 1.20 per cent Mn, 0.15 per cent carbon steel made with a basic single slag practice and a Si block 8 min before tap. An addition of 1.30 per cent Mn to a residual of 0.20 per cent yielded a final of 1.19 per cent Mn for an overall recovery of 80 per cent. In spite of the lower carbon, extreme early oxidation, and very high tap temperature, the proportionate loss on this higher Mn steel was no greater than that averaged on 0.75 per cent Mn, basic single slag heats. The slag MnO increase was greater than that experienced on lower Mn basic heats but nothing like the increase experienced on acid heats where twice that addition of Mn would have been necessary.

A comparison of basic and acid experiences suggests the following conclusions on basic practice. Much higher Mn recovery is experienced under a basic slag with equivalent practice. Where single slag acid heats on 0.75 per cent Mn steel have experienced an average overall recovery of 60 per cent, single slag basic heats have averaged 80 per cent. Because of this lower recovery of added Mn, plus the lower residual Mn retained from the charge, acid heats require Mn additions 1½ to twice as large as basic heats for the

same final Mn. On higher Mn steels, basic heats do not seem to experience the greater proportionate losses and the increased difficulty in control encountered on acid heats.

In basic-lined furnaces, reducing second slags can be added that practically eliminate slag FeO and Mn loss. The lower FeO of an equivalent basic slag causes less loss of deoxidants and a relatively small increase in slag MnO. Indications that the basic slag does not have the inherent affinity for MnO of the acid slag are further evidenced by the higher Mn

(MnO) ratio obtained after equiresidual and lower

valent oxidation.

ferred.

Acid Practice, Low FeO Double Slags

Further experimentation was directed toward the effect of variations of slag composition. Extremes of slag composition were obtained by draining off the original slag and adding a synthetic mixture of predetermined proportions. Every attempt was made to keep a uniform "volume of slag" on these double slag heats. The total weight of all slag making equiptions was 21/2 to 3 per cent of the weight of the metal charge. This addition plus some original slag missed in draining and the usual increases from refractories while finishing the heat made final slag-to-metal ratios range between 4 and 5 per cent. With single s'ag practice, variations in slag ratios from 21/2 to 9 per cent have been detected and have apparently added to the complexity of accurate Mn control.

In regular practice, the slag constituent that has exerted the most conspicuous effect on Mn recovery has been iron oxide. Working the original slag to an extremely low FeO content prolongs the heat and requires reducing conditions for a length of time that adversely affects dutility.9 The addition of a second slag seems preferable when such a low slag FeO is pre-

On Heat 9 (in Table 1 and below) the slag was drained as clean as practical and a second slag mixture of 40 lb of dolomite and 80 lb of sand was added (for a 4000-lb charge). The MnO content of 6.3 per cent indicates that 35 to 40 per cent of the original slag was left in the furnace. Calculated on this basis, the FeO of this second slag should have been 9 to 10 per cent. Actually the FeO analysis was 13.4 per cent after a Si block. An addition of 1.90 per cent Mn yielded a final of 1.42 per cent for a 71 per cent recovery. After some allowance for a colder than average tap temperature, this represents a considerably improved recovery for a final Mn this high. The changes in composition of this slag are shown below. In spite of a much lower slag FeO before the Mn addition, the final slag FeO content seemed to stop at the same level of about 6 to 8 per cent. Although the slag FeO was lowered only 5.4 per cent, the slag MnO increased

On this heat, the SiO₂ of the slag was decreased 8.8 per cent. Because of the lower FeO, less of the reaction seemed to be borne by the FeO and more by the SiO2-Mn reaction. Under conditions of low slag FeO and high metal Mn, the Mn loss did not continue to follow

LOW FEO DOUBLE AND TRIPLE SLAG HEATS

| | % Mn | | Slag Analysis, % | | | | |
|--------------------------|---------|---------------------|--------------------------------|--------|-------|------|-----|
| | In Meta | al SiO ₂ | Al ₂ O ₃ | FeO | MnO | CaO | MgO |
| Heat 9 | (55186) | (2 Slag) | (1 D | ol - 2 | Sand) | | |
| Before Mn Addition | n 0.11 | 65.6 | 1.8 | 13.4 | 6.3 | 7.0 | 4.8 |
| Amount Added | (1.90) | | | | | | |
| Before Tap | | 61.5 | 2.5 | 11.1 | 11.3 | 7.0 | 5.1 |
| Ladle Sample | 1.42 | 56.8 | 1.8 | 8.0 | 20.9 | 6.0 | 4.2 |
| Heat 10 (45056) (2 slag | | (2 slag) | (2 Dol - 3 Sand) | | | | |
| Before Mn-Si black | 0.12 | 58.6 | 1.2 | 12.7 | 10.4 | 9.5 | 7.0 |
| Before final Mn | | 62.0 | 1.9 | 9.4 | 11.5 | 8.7 | 6.6 |
| Amount added (total | (1.80) | | | | | | |
| Ladle sample | 1.20 | 55.6 | 2.7 | 6.4 | 19.4 | 7.6 | 6.0 |
| Heat 11 (35051) (3 slag) | | (2 Dol - 3 Sand) | | | | | |
| Before Mn addition | 1 | 65.8 | 1.4 | 11.2 | 3.9 | 10.9 | 7.5 |
| Amount Added | (1.50) | | | | | | |
| Before tap | 1.27 | | | | | | |
| Ladle sample | 1.03 | 58.6 | 1.7 | 6.6 | 18.5 | 8.1 | 6.3 |

slag FeO. Heat 10 was another double slag heat similar to Heat 9 except for a Mn and Si block rather than a straight Si block. Recovery was 62 per cent

and slag changes were similar.

To investigate the effect of a still lower slag FeO a three slag heat was run (Heat 11). A second slag was added and drained after a few minutes. Then a third slag of dolomite and sand was added. Calculations indicated the FeO of the third slag should have been 4 to 6 per cent. FeO analysis of this slag was 11.2 per cent FeO, not much lower than the double slag. This suggests that lower FeO acid slags can not be maintained over a metal bath that has not been deoxidized to an equivalent extent. Essentially no improvement in Mn recovery was experienced over the double slag heat. The 1.50 per cent addition yielded a final Mn of 1.03 per cent for a 65 per cent recovery. Again the decrease in slag FeO accounted for only about one third and the silica reaction accounted for about two thirds of the Mn lost from the metal and the MnO gained by the slag.

It seems significant that the final equilibrium slags of all three heats attained practically the same FeO contents within themselves and compared closely with the single slag heats. Also of some possible significance is the apparent tendency for the MnO contents to reach the same final value under similar conditions.

Certain conclusions are evident from these experiments. By using a double slag practice, slag FeO can be reduced readily and some improvement in Mn recovery accomplished. With further decreases in slag FeO, proportionate improvements in recovery are not experienced on higher Mn steels. Apparently a large increase in Mn in the steel requires a comparable increase in MnO in the slag to satisfy the acid-base relationship. The MnO is produced by the Mn-silica reaction if the necessary FeO is not available.

High Manganese Oxide Slags

Since high Mn steels seem to require high MnO slags for equilibrium, an attempt was made to satisfy this condition artificially by the addition of manganese oxide ore to the slag. This should reduce the need of consuming Mn from the metal. On Heat 12 sufficient Mn oxide was added to the slag 2 min before the Mn addition to increase the slag MnO from 14.8

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to 21.2 per cent. The 1.50 per cent Mn added produced a final of 1.06 per cent. This 66 per cent recovery is very good considering the increased quantity of slag from the addition. With a fluidity of 4.5 in. this slag of larger volume gave better recovery than some of the 1-in. slags. Of course, the fluidity of this slag was due to a high concentration of MnO rather than FeO.

SLAGS WITH INCREASED MNO

| | % F | | · SiO ₂ | | | ysis, % MnO (| | MgO |
|------------------|--------------|-------|--------------------|------|--------|------------------|-----|-----|
| . Heat | 12 (35039) | (Sir | ngle SI | ag + | - MnC | 0) | | |
| Ore down | 0.12 | 6.0 | 49.2 | 2.2 | 22.7 | 14.8 | 5.5 | 3.5 |
| MnO added | | | | | | | | |
| Before Mn | 0.13 | 4.5 | 52.2 | 3.1 | 14.4 | 21.2 | 5.4 | 3.4 |
| Amount Mn added | (1.50) | | | | | | | |
| Tapped 3200° (Al |) | | | | | | | |
| Ladle Sample | 1.06 | | 51.0 | 3.8 | 6.4 | 29.4 | 4.7 | 3.0 |
| Heat 13 (3- | 1046) (2 sl | ag) (| 3 Mn | 0, 1 | Dol. | Sand |) | |
| Before Mn | | 1.7 | 64.3 | 1.3 | 12.1 | 14.1 | 3.4 | 2.0 |
| Mn added | (1.50) | | | | | | | |
| Before tapping | , , | | | | | | | |
| 3200° (Al) | 1.20 | | | | | | | |
| Ladle sample | 1.03 | | 53.4 | 5.2 | 6.2 | 27.6 | 3.6 | 3.4 |
| Heat 14 (35 | 6049) (2 sla | ag) (| 3 Mn | 0. 3 | Dol. 5 | Sand |) | |
| Before Mn | , , | | | | | 12.1 | | 5.0 |
| Mn added | (1.50) | | | | | | | |
| Tap 3200° (Al) | 1.12 | | | | | | | |
| Ladle sample | 1.03 | | 55.4 | 5.3 | 7.7 | 21.3 | 6.5 | 4.7 |

MnO was used as the basic ingredient of second slag mixtures both alone and with dolomite. On Heats 13 and 14 an attempt was made to add a second slag mixture low in FeO but with normal contents of the other basic constituents. Heat 14 was intended to be slightly more basic. These artificial slags were successfully obtained very similar to low FeO single slags. Both heats experienced identical recoveries and the same as the dolomite-sand double slags.

Heat 15 represents an attempt toward a very high MnO second slag, theoretically calculated to 30 per cent MnO, and supposedly high enough to not require any Mn loss to reach equilibrium with the final Mn expected.

VERY HIGH MNO SECOND SLAG

Slag Analysis, %

Mn, Slag

| | % Fluid- SiO ₂ Al ₂ O ₃ FeO MnO CaO Mg ity, in. | | | | | | | | | | | | |
|--------------|--|--------|--------|-----|--------|------|-----|-----|--|--|--|--|--|
| Heat 1 | 5 (35038) | (2 sla | ig) (5 | MnO | , 3 sa | nd) | | | | | | | |
| Before Mn | | 4.5 | 55.5 | 0.2 | 18.7 | 18.3 | 4.8 | 2.8 | | | | | |
| Mn added | (1.50) |) | | | | | | | | | | | |
| Tap (3350°) | 1.24 | | | | | | | | | | | | |
| Ladle Sample | 0.88 | | 53.8 | 0.6 | 9.8 | 28.4 | 1.8 | 0.5 | | | | | |

However, the slag actually reached only 18.3 per cent MnO. Apparently, excessive MnO fluxed away acid refractories into the slag increasing the slag volume and preventing such an excessive concentration. Also, a higher FeO content was retained in this highest MnO slag. Whether this was due to some equilibrium tendency for high slag MnO to retain more FeO or simply from FeO impurities in the manganese ore was not determined. In view of the highest tap temperature of all heats, the 0.88 per cent Mn from a 1.50 per cent addition was not necessarily low, but was no im-

provement over second slags with less MnO. There seems to be a practical limit to the slag MnO, which was exceeded in this case.

On Heat 16 an attempt was made to defeat these apparent inclinations by adding a second slag of dolomite and sand and later making a large addition of MnO (2 per cent of metal weight) just before the addition of the Mn to the bath. Comparable recovery resulted and no better than would have resulted if the late MnO had not been added. The increased slag quantity seemed to offset the reduced MnO increment required for equilibrium.

| | % | | - SiO ₂ | | Analy: FeO M | | aO N | иgО |
|--------------------|--------|--------|--------------------|-------|-----------------|-------|------|-----|
| Heat 16 (350 | 50) (2 | Slag) | (Dol, | Sand, | MnO | late) | | |
| Before Mn | | 3.0 | 56.5 | 0.0 | 12.3 | 15.8 | 7.9 | 5.0 |
| Mn Added | (1.50) |) | | | | | | |
| Tap (3200°) (Al) | 1.16 | | | | | | | |
| Ladle Sample | 0.98 | | 54.5 | 3.8 | 8.0 | 22.1 | 6.6 | 4.5 |
| Heat 17 (3 | 5729) | (2 Sla | (g) (2 | MnO | . 3 Sa | nd) | | |
| Before Mn-Si block | | | | | 18.0 | | 3.9 | 2.3 |
| Before final Mn | | 3.0 | 56.8 | 0.9 | 14.4 | 20.9 | 3.8 | 1.8 |
| Total Mn added | (2.30) | | | | | | | |
| Tap (3180°) | | | | | | | | |
| Ladle Sample | 1.55 | | 51.8 | 0.7 | 6.7 | 33.7 | 3.5 | 1.9 |

Heat 17 represents what was found to be the most practical proportion of MnO in a second slag, 21 per cent MnO and 3-in. fluidity. This procedure was used in the production of high Mn steels giving better control and higher recovery than the usual single slag practice.

Experience with increased MnO slag contents suggests the following conclusions. Recovery of Mn has been improved by increasing the slag MnO, either by manganese oxide additions to the original slag or with MnO as the principal basic constituent of second slag mixtures. Beyond a certain practical limit, attempts to further increase slag MnO and Mn recovery were unsuccessful. Slags of excessive basicity seem inclined to dissolve acid refractories too rapidly to be maintained in an acid furnace. The high MnO slags have much higher fluidity for their recovery. They also hold a steady arc permitting efficient attainment of high temperature metal.

Effect of Do'omite and Lime

Some plants make slag additions of limestone in which CaO is the active slag ingredient. Others use do!omite which gives a combination of CaO and MgO. An attempt was made to compare the effect of these apparently interchangeable materials as well as their general effect on Mn recovery. On Heat 18 (Table 1) a second slag was made of dolomite and sand, and on Heat 19 a second slag of lime, and sand so proportioned to give essentially the same (CaO + MgO) content in both. Otherwise the two slags were almost identical in composition. To both heats 1.50 per cent Mn was added and on both heats 1.01 per cent Mn was obtained for a 62 per cent recovery. On another pair of heats, the one using lime showed practically no difference in Mn recovery from the heat using a chemically equivalent quantity of dolomite. In the proportions used in acid slags CaO and MgO seem

chemically interchangeable for all practical purposes in the Mn reaction. In subsequent discussions (CaO + MgO) content will be discussed as one variable in

composition.

Double slag experimental heats were run varying the (CaO + MgO) contents of the slag over the entire range of practicality. To determine the effect of practically no (CaO + MgO), Heats 22 and 23 were run. Second slags of mill scale (iron oxide) and sand were added, so proportioned to give a slag of high fluidity on Heat 22 and a low fluidity on Heat 23. This procedure made both second slags low in MnO also, so that the effect noted was actually that of low CaO, MgO, and MnO with FeO increased to become a more prominent basic constituent than usual. With a 41/2 in. slag on Heat 22 the 1.50 Mn added retained a final of only 0.74 per cent Mn for a recovery of 45 per cent. Heat 23 with more sand and less mill scale had a fluidity of only 11/2 in. in spite of a high FeO content of 30.6 per cent. From the 1.50 per cent added, only 0.86 per cent final Mn resulted from this heat for a recovery of 53 per cent. These were the lowest recoveries experienced on any slags of equivalent fluidities and temperatures.

With both FeO and MnO decreased, second slags of the dolomite-sand type seem to require a minimum of 12 to 14 per cent (CaO + MgO) to obtain a workable fluidity. Heats 9 and 20 are representative of this practical limit. On Heat 9 a Mn recovery of 71 per cent was obtained with a lower than average temperature and on Heat 20 a 64 per cent recovery with an average temperature. (CaO + MgO) contents in the range of 15.3 to 18.4 per cent were used on Heats 10, 18, and 19. Mn recoveries were all 62 per cent, showing no significant difference in recovery from these variations in (CaO + MgO) content.

Heats 24 and 25 are double slag heats in a lower Mn range. Although Heat 25 had a slag of 5 per cent higher CaO + MgO content and twice the fluidity,

Mn recoveries were identical.

On one heat (Heat 21), the maximum proportion of dolomite considered practical was used on the second slag, 3 parts dolomite and 2 parts sand. This mixture was calculated to give about 30 per cent (CaO + MgO) and about 50 per cent total bases in the slag.

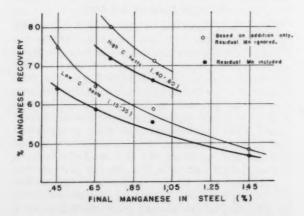


Fig. 4-Manganese Recovery vs. Final Manganese in Steel.

Again as in the case of the high MnO content, this excess basicity could not be maintained in an acid furnace. At the time of the Mn addition the (CaO + MgO) content was only 21.1 per cent. On this heat the 1.50 per cent Mn added gave a final of 0.89 per cent for a 60 per cent recovery. Correcting for the high temperature of 3350 F, the recovery was not much lower, if any, than the lower lime slags, but certainly no better.

It seems significant that the highest MnO slag of 4.5 in. fluidity (Heat 15) and the highest (CaO + MgO) slag of 4.0 in. fluidity (Heat 21) experienced 54 and 56 per cent recoveries respectively at the same temperature of 3350 F. On the other hand, a 4.5 in. slag (Heat 22) with a high FeO but low in all the other basic constituents, obtained only 45 per cent

recovery at a much lower temperature.

CaO seems to be able to substitute for MnO in the requirements for slag equilibrium with the finished steel. Comparable improvements in Mn recovery have been obtained with lime increases as with MnO increases in the slag. In the amounts used, no appreciable difference was found between CaO from lime and (CaO + MgO) from dolomite when in chemically equivalent proportions. The effect of MgO on Mn recovery seems essentially the same as CaO.

Up to a certain point, lime seems to replace FeO and improve Mn recovery without affecting fluidity appreciably. Beyond that amount further increases of lime increase the fluidity but affect Mn recovery very little in relation to the effect on fluidity. Low lime, low MnO slags thicken up fast on a high temperature heat because the principal base FeO is being rapidly consumed. By virtue of its inactivity toward any reactions, lime tends to stabilize slag composition and maintain fluidity.

Effect of Final Steel Composition

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Among steels of the same composition, Mn loss seems dependent chiefly upon slag composition. However, with the same slag analysis, steels of different composition types have not been found to experience the same Mn loss. The Mn reaction seems to be affected by the final chemistry of steel, the C and Si as well as the Mn.

The most important element is the final Mn content of the steel. From records kept of Mn recoveries and slag conditions of a number of types of steel, the Mn recovery is plotted against the final Mn of the steels in Fig. 4. Each point represents an average of a number of heats. One curve is drawn for low C heats (0.20–0.35 C) and another curve for higher carbon heats (0.40–0.60 C). From these curves it is apparent that as the desired final Mn increases, the proportionate Mn loss actually increases.

Also apparent from Fig. 4 is the effect of carbon content of the steel. In the same Mn range, higher carbon heats experienced higher Mn recovery. Higher carbon heats are inclined toward a lower FeO content and more viscous slags, may be tapped colder, and are sometimes run by a slightly different furnace practice. All of these factors may contribute toward a higher

recovery.

Since most specifications for cast steel require prac-

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tically the same range of silicon, less opportunity was afforded to study the effect of this element. However, it was noticed early that with apparently the same oxidation history and slag composition, high silicon (0.50–0.80 per cent), high Mn heats experienced better recovery than low silicon, high Mn heats of the same carbon content. Some attempted low Si heats have not only lost more Mn but picked up more Si. In fact, we have found it impossible to make high Mn steels very low in silicon in an acid furnace at high temperature. If the silicon is not added it is obtained by reduction at the expense of Mn.

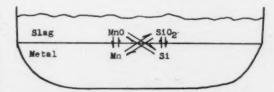
This tendency is best illustrated by the difficulty experienced in making a 0.30 per cent max Si, 1.20 per cent Mn steel at high temperature in an acid furnace. On the first attempt by regular practice no silicon was added, yet a too high silicon of 0.40 per cent resulted at the 3250 F temperature required. The samples below indicate that 0.36 per cent Si was reduced from slag. Actually 0.32 per cent Si was reduced after the Mn addition and of this amount 0.18 per cent Si was picked up simply in pouring into the ladle. Some of the Si from this particular heat could have resulted from some chromium reaction. However, similar Si "pick-ups" have been encountered from heats with no chromium.

ATTEMPTED LOW SI, HIGH MN HEAT

| | Si | Mn | Cr | C |
|----------------------------|------|--------|--------|------|
| After violent boil | 0.04 | 0.15 | 0.33 | 0.17 |
| Before Mn and Cr | 0.08 | 0.14 | | |
| Additions | (0) | (2.20) | (0.77) | |
| In furnace at tap | 0.22 | 1.52 | 1.07 | |
| Ladle sample after tapping | 0.40 | 1.22 | 0.96 | 0.38 |

A given Mn level seems to require a certain minimum silicon at a given temperature. If this silicon is not added, a reduction reaction seems to try to supply it at the expense of the Mn until a balance is reached. This seems to be further evidence of the manganese-silica reaction,

$$\mathrm{Mn}_{(m)} + \mathrm{SiO}_{2(s)} = \mathrm{MnO}_{(s)} + \mathrm{Si}_{(m)}$$
 and a tendency to reach an equilibrium between the acid and basic elements in the metal with their acid and basic oxides in the slag as indicated below.



From the principles of chemical equilibrium, the forward reaction should be retarded by an increase in concentration of the reaction products on the right. Experience has proven that the Mn consuming reaction has been decreased by increasing the concentration of MnO in the slag. The reaction has also been restrained by increasing the concentration of the other product on the right, the Si of the metal. From the same law of physical chemistry it stands to reason that an increase in concentration of one of the products decreases the necessary concentration of the other

product required to reach equilibrium. Therefore, an increase in MnO of the slag should decrease the Si concentration necessary for equilibrium and permit production of a high Mn steel with lower Si content.

This theory was given a trial. A second slag of MnO and sand was used that ran 15 per cent FeO and 20 per cent MnO. With no silicon added, an acceptable final of 0.30 per cent resulted at the same tap temperature. This was 10 points lower than previously obtained and barely within specification. A second heat was run in the same way and finished with a still lower 0.28 per cent Si from no addition. Both heats had slag fluidities of 3 to 4 in. before the Mn additions and tapped at 3250 F. This particular problem was solved by such application of the mass action law.

It is now evident that the very low recoveries of 38, 39 and 40 per cent experienced on the high Mn Heats 6, 7, and 8 in Table 1 were caused partly by attempts to keep the final silicon below 0.45 per cent to stay within a specification apparently established for basic steels. Holding the silicon additions down to 0.10 to 0.20 per cent was not effective since an additional 0.26 to 0.29 per cent Si was obtained by the reduction reaction. When later specifications permitted a higher final silicon and much higher Si additions were made, proportionately less Si was reduced and Mn recoveries were increased to 50 to 55 per cent under otherwise the same conditions. Recent research has indicated that higher silicon contents are generally beneficial to the physical quality of cast steels.¹⁰ Considerable evidence indicates that a steel with the Si content increased in proportion to the Mn content is less corrosive to refractories in the furnace and the ladle and retains fewer inclusions than a composition in which the Mn/Si ratio is higher. Higher Si specifications on high Mn cast steels are advantageous in steel melting as well as in final quality.

Conclusions on steel composition may be summarized as follows. Proportionate Mn loss has been found to increase as the Mn of the steel increases. Higher carbon heats are inclined toward better Mn recovery. Silicon seems to be involved with Mn in the equilibrium set up, and silicon content influences Mn recovery beyond the effect of deoxidation. It proved impossible to obtain a silicon below a certain minimum in equilibrium with a high Mn steel for a given temperature. If silicon is not added to the bath it is reduced from the slag until equilibrium has been reached. Better Mn recovery can be obtained if the maximum permissible Si content is obtained and the addition made with or before the Mn addition rather than after it.

Final Slag Composition

The variation of final ladle slags with various steel types was studied. The near absence of any variation in final FeO content suggests that the slag attempts to approach an FeO content in equilibrium with the deoxidizing elements in the steel. Higher C, Mn, Si, and Al in the metal all seem to slightly lower the final slag FeO but determination of their relative effects was not attempted.

Attempts to plot final slag MnO against Mn content of the steel encountered considerable discrepancies on heats with unusual lime contents. The best correla-

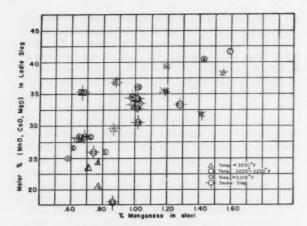


Fig. 5-Relation of Manganese in Steel to Slag Composition, Both Sampled from Ladle after Manganese Reaction.

tion was obtained by plotting Mn in the steel against (MnO + CaO + MgO) contents of the final slag calculated to molar per cents. This relation is graphed in Fig. 5. The heats tapped at similar temperatures show a fair degree of correlation considering the wide range of experimental slags included, variations in slag volume, and inevitable errors in sampling and analyses. The heats of "average" tap temperatures, represented by circles, line up fairly well in the middle of the group showing proportional increases of slag basicity with increases in metal Mn. Almost all the higher temperatures heats (squares) line up above the average line indicating further reaction and greater Mn loss at higher temperature. Most of the lower temperature heats (triangles) are below the average group. Double slags proved no different from single slags on this graph of the metal-slag relationship after the Mn reaction.

These results seem to indicate that final slag equilibrium is influenced by the Mn content of the metal, its basicity increasing proportionately with the final Mn. This relationship theoretically justifies the large Mn losses required to reach or approach equilibrium on higher Mn steels and further confirms the improvement in recovery and decrease in final slag MnO resulting from increased lime or dolomite additions to

the slag.

Effect of Temperature

Early experience seemed to indicate that heats tapped at higher temperatures had greater Mn losses. Some confirmation of this effect was indicated in Fig. 5. When a double slag practice was standardized for high Mn heats, optical pyrometer readings of the tapping stream were determined and recorded. After a few heats, a relationship was used on subsequent heats as a basis for the adjustment of the Mn additions. With the accumulation of more data the curve was further substantiated. This temperature effect is shown in Fig. 6. Considering the low degree of accuracy in the method of temperature measurement and the unavoidable variations in slag ratio and composition, this trend seems rather significant. It was not deter-

mined whether the effect of the temperature is chiefly from a shift in the equilibrium or due simply to a closer approach to equilibrium made possible by the increased activity of the system.

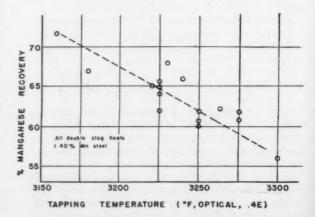
It is now evident that much of the early trouble experienced with low recovery and poor control on the 1.4 per cent Mn steels was caused by the high temperatures of 3200 to 3300 F required. If the heats could have been tapped at 3050 to 3100 F, recoveries averaging 65 or 70 per cent, might have been experienced instead of 40 to 50 per cent. Control probably would have been better and the Mn loss never have attracted attention.

Conclusions on the effect of temperature on Mn recovery may be summarized briefly. With similar slag composition and slag to metal ratio, Mn loss was found to increase with increased tap temperatures. The effect of temperature seems more important on acid heats and on high Mn steels. By taking optical readings in a spoon before tap and a reading of the stream while tapping, a temperature recovery graph has been used to adjust the Mn addition and thereby improve Mn control.

Effect of Slag Fluidity

The development of a viscosity mold by Herty¹ made possible the first rapid quantitative expression of a slag property. The fluidity of an acid slag has been found closely related to its composition. Juppenlatz² described the application of such a viscosimeter to acid electric slags, stressing the importance of slag control and giving some fluidity-composition relationships. Bowers3 described improvement in Mn control experienced through the application of slag fluidity measurements to adjust Mn additions. The acid open hearth Research Committee contributed a considerable volume of data on the fluidity-composition relationships found in acid open hearth practices.4 Johnson, McDonough, and Radford⁵ have recently reported more fluidity data on acid electric slag. In their plant, fluidity measurements were used to supplement observation of slag color for improved Mn control.

All the published data seem to indicate that, at a constant temperature, acid slag fluidity is an inverse function of the silica content or a direct function of



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Fig. 6-Relation of Tap Temperature and Manganese Recovery.

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the total bases in the slag. Since FeO is the most frequent basic variable affecting slag fluidity, fluidity variations generally reflect variations in FeO. When such is the case and other variables are excluded the fluidity measurement has proved a useful indication of Mn loss. However, many exceptions to this relationship have been experienced and a few widely scattered points on all the published graphs indicate the effect of some other variables. Many of these factors have been qualitatively suggested in the literature but without much data on the magnitude of their effect.

An attempt to graph fluidity vs recovery on all the widely varied slag compositions in this study resulted in a "shot gun pattern" indicating no general correlation under such a variation of basic constituents. In a discussion of slag fluidity it seems necessary to separate two distinct influences attributed to fluidity: (1) The strictly physical effect of fluidity within itself. (2) The effect of slag composition simply indicated by fluidity.

It seems evident that an increase in slag fluidity, with no effect on composition, would increase the diffusion rate and general reactivity of any two phase reaction, and any acceleration of the slag-metal reaction would tend to increase the Mn loss.

The second influence concerns not a direct effect of fluidity, but the use of fluidity measurements to indicate slag composition. In this case slag composition is the factor actually affecting the recovery. Increased FeO increases fluidity and Mn loss proportionally. However, other basic constituents like MnO, CaO, and MgO increase fluidity but not Mn loss. Recoverý may be actually improved. When standardization of these other basic slag constituents can be assured and FeO is the principal basic variable, then fluidity measurements can be used to indicate Mn loss with reasonable accuracy, provided the temperature is controlled, and the Mn is not high enough or the slag FeO low enough for the manganese-silica reaction to take place.

The appearance of acid slags is very sensitive to variations in composition. Careful observation of luster, color shade, and color intensity makes it possible for the experienced melter to estimate the effective slag composition and its influence on the required Mn consumption. When supplemented by such means of detecting unusual variations of other basic constituents, and preceded by an understanding of the type of steel and its equilibrium tendencies, slag fluidity measurements can assist the melter in estimating Mn recovery.

Production Experience with Double Slag Practice

On 1.4 per cent Mn heats, improved Mn recovery and control were realized in production from the use of a double slag practice. Actually two different double slag mixtures were used with essentially equal success. One was a mixture of 1 part manganese ore and 2 parts sand, and the other a combination of 2 parts dolomite and 3 parts sand. The total weight of slag making materials was made to equal 21/2 per cent of the metal charge weight. After the boil, the oxidizing slag was drained and the second slag mixture added. The Mn and Si additions were withheld until near tap time. The Mn addition was adjusted according to temperature indications on the basis of the temperature-recovery graph in Fig. 6. Recoveries of

60 to 70 per cent were generally experienced. Mn control was improved considerably over the conventional single slag practice. The last 40 heats made by the double slag process were within the allowable 30 points of Mn range.

Equivalent slag composition and recovery can and has been obtained on certain single slag heats without the necessity of the second slag. However, on the double slag the composition is generally more favorable and the quantity of slag is less than usually encountered on single slag heats, accounting for the generally improved recovery. The consistency of slag quantity and composition on the double slag heats accounts for the decidedly improved control.

The MnO-sand slag holds a steadier arc but has the disadvantage that its appearance is less sensitive to variations in composition. The color imparted to the slag by the MnO is similar to that of FeO, making it more difficult to recognize irregularities in composition. On the other hand, the dolomite-sand slag is very sensitive to variations in composition and chemical uniformity from heat to heat can be assured by observing the slag color and luster.

When a double slag is used to effect earlier and more complete deoxidation, considerable evidence has indicated impairment of ductility. However, on these heats the second slag was added to a well oxidized bath and deoxidizers were withheld until a few minutes before tap. All these double slag heats passed the physical specifications with properties equivalent to single slag heats and apparently were not affected adversely by this slag practice.

Control of Slag Composition Single Slag Practice

The importance of uniformity of slag quantity and composition has been emphasized by the experience with double slag heats. However, with the single slag practices generally used, control of these factors is more involved than simply weighing the slag mixture to be added. In many cases an unusual slag quantity or composition cannot be corrected, but can be compensated for, if properly recognized.

Slag FeO is most sensitive to the progress of the heat and can be controlled to almost any content by an experienced melter. The maximum FeO during the oxidation period depends on the amount of oxidizing agent added. The rate of decrease toward the final content caused by the carbon boil and deoxidation reaction is influenced by the oxidation history, the rate of heat imput, deoxidizers added, other basic constituents of the slag, the time and sequences of operations, etc.

In the acid furnace, slag SiO₂ is indirectly determined by the basic constituents with a tendency to increase in concentration, as the heat progresses, the FeO is reduced, and SiO₂ from the refractories enters the

The sources of the small amount of Al₂O₃ usually present are: oxidation of aluminum from killed and cast steels in the charge, Al₂O₃ as impurity in ore, and occasionally clay from some refractories. The alumina is not reactive and simply decreases in concentration by dilution as the heat proceeds and the quantity of slag is increased by added SiO₂. Upon

tapping, the increases in alumina concentration of 2 to 5 per cent are experienced when aluminum is added

to the tapping ladle.

The source of the CaO and MgO contents is the limestone or dolomite addition. The addition can be carefully weighed and standardized, but 50 per cent variations in total slag quantity can cause wide deviations in concentration. Real control of CaO concentration requires adjusting the addition according to estimated or expected slag quantity, which in turn requires recognition of the factors influencing slag to metal ratio. CaO and MgO concentrations decrease as the heat progresses and slag quantity increases.

The importance of manganese oxide concentration in the slag has been repeatedly emphasized. Variations in this constituent have proven more difficult to recognize and control. Obviously the source of the MnO is the Mn oxidized from the charge, and the amount transferred to the slag depends upon the Mn available in the charge and the degree of oxidation experienced. The table below shows how slag MnO content may vary from 7.8 to 20.6 per cent as the Mn of the charge varies from 0.40 to 1.00 per cent Mn, assuming the same quantity of slag. Obviously, the first step in MnO control is control of charge makeup.

EFFECT OF MN OF CHARGE ON MNO SLAG
(CALCULATED)
(Assuming 5 per cent slag/metal ratio. Based on 1000 lb Metal)

| Mn Content of Charge, % | Likely Residual, | Wt. of Mn Oxidized, lb. | | Resulting MnO Concentration, |
|-------------------------------|---------------------|-------------------------------|------|---------------------------------|
| 0.40 | 0.10 | 3 | 3.9 | 7.8 |
| 0.65 | 0.15 | 5 | 6.5 | 13.0 |
| 1.00 | 0.20 | 8 | 10.3 | 20.6 |

However, with identical charges and the same amount of Mn oxidized, variations in slag quantity from 3 to 8 per cent can cause variations in the MnO concentration from 21.6 per cent to 8.1 per cent as indicated in the table below. A second factor of equal importance is the control of the total quantity of slag into which the standardized quantity of MnO and CaO will enter.

EFFECT OF SLAG QUANTITY ON MNO OF SLAG (CALCULATED)
(Assuming same charge oxidized from 0.65 to 0.15%)
(1000-lb Charge)

| Slag Metal Ratio, % | | | | Resulting MnO Concentration, % |
|---------------------------|----|-----|-----|--------------------------------------|
| 3 | 30 | 5.0 | 6.5 | 21.6 |
| 5 | 50 | 5.0 | 6.5 | 13.0 |
| 8 | 80 | 5.0 | 6.5 | 8.1 |

All these extremes have been experienced in actual practice. When slag quantity is unusually large or Mn content of the charge is unusually low, slag analyses have indicated lower than average MnO concentrations and greater Mn losses have been experienced.

Control or estimation of slag quantity is difficult because of the large number of variables involved. Only the slag obtained from the oxidizing and slag making additions can be determined with any accuracy. Rust and dirt on the steel scrap and unfused sand

used to patch the hearth add to the slag volume in an indeterminate manner. The amount of silica fused from the furnace refractories during a heat is a major fraction of the slag. This refractory consumption is influenced by the degree of oxidation during melting, slag basicity, the hearth shape and dimensions, the manner of melt down, length of heat, rate and manner of heat imput, metal temperature, etc.

Variations of certain of these factors are unavoidable when a furnace is relined. It was recognized quite early that a newly relined furnace generally experienced 10 to 15 per cent better Mn recovery than the same furnace just before relining. Differences in slag appearance were also recognized. Slag studies have made the reasons apparent. A size "Q" furnace has a 13-in. side wall lining and a bath diameter at the slag line of 57 in. With use, the side walls are consumed to an approximate thickness of 51/2 in. before relining. This 15-in. increase in diameter at the slag line increases the surface area of the slag from 2520 to 4100 in. The effect on the diameter-to-depth ratio and consequently the melt down characteristics is apparent; and more patching sand is naturally used for the increased area. The two heats below, melting 7500-lb charges in this furnace, illustrate the effect of relining on slag quantity, slag composition, and ultimate Mn recovery. The 185th heat made on the last day of the old lining had a slag ratio of 6.5 per cent, compared to a 3.6 per cent ratio on the third heat of the new lining.

| | | | | | | | | Sla | Befo | npos re M | ition n | 1 |
|-----|----------------------|-----------------|------------------|-------------|------------------|----|------------------|--------------------------------|------|--------------|------------|-----|
| | Approx. Diam, in. | Wt. Slag, 1b | Slag Ratio. % | Tap Temp, F | Slag Vis, in. | Mn | SiO ₃ | Al ₂ O ₈ | FeO | MnO | CaO | MgO |
| day | av im | 080 | 9.0 | 3300 | 2./ | cc | ×9.0 | 0.0 | 100 | 176 | 9.9 | 9.8 |

This difference in slag quantity caused a variation in MnO content from 10.3 per cent to 17.6 per cent from the same charge. When proper allowance is made for the 120-deg higher temperature, Mn recovery was much better on the new lining in spite of the higher slag fluidity. Increases in diameter to depth ratio within the same size furnace seem to increase the slag weight ratio. However, increases in the overall size of the furnace and the charge seem to decrease the relative quantity of slag.

Where slag quantities from a number of heats have been plotted against Mn recovery, some degree of correlation has been apparent in spite of the number of variables involved. Increased quantities of slag generally decrease Mn recovery, indirectly by diluting MnO and CaO and producing a less favorable slag composition, and directly by increasing the total amount of MnO required to satisfy the greater volume of slag. Control of Mn on single slag heats suggests control of, or allowance for, slag quantity as well as slag composition.

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Obviously, the most desirable approach toward improved control is empirical standardization of all influencing factors so that Mn recovery will be essentially EL

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identical from heat to heat. However, frequent irregularities in the charge and bath dimension along with special nonrepetitive types of steel make complete standardization difficult and require considerable adjustment of Mn addition on some heats to obtain the desired final chemistry. Improvement in Mn control can and has been accomplished in the single slag acid electric practice by the last minute adjustment of the Mn addition based on consideration of the following factors: (1) the type of steel being made and its characteristic equilibrium tendencies, (2) the type of charge, especially average Mn content, (3) the degree of oxidation of the charge and the resulting residual contents of Mn, Si, C, and FeO, (4) the fluidity of the slag before Mn addition, (7) the composition of the slag before the Mn addition as indicated by the appearance and fluidity, and (8) metal tap temperature.

Effect of Use of Oxygen on Mn Reaction

The recent substitution of oxygen for ore as a decarburizing means has raised some questions on Mn recovery and control. Some reports from some of the first heats using oxygen claimed excessive Mn losses. Other reports have indicated improved Mn recovery from the use of oxygen. A number of heats using both oxygen and ore were compared from this standpoint and attempts made to explain any differences experienced.

Table 3 contains four typical heats on which oxygen was used and two on which ore was used. Metal charges were from the same lot of scrap shells and were chemically and physically identical. Charge analysis was 0.20 per cent Si, 0.70 per cent Mn and 0.50 per cent C. All heats were of the same SAE 1040 steel and melted in the same furnace, equally loaded and in the same refractory condition. After melting, oxygen was applied in the quantities indicated and at the temperatures noted. The oxygen was introduced at the rate of 75 cu ft per min through a \(\frac{3}{6} \)-in. protected pipe at 40 psi pressure.

After removal of at least 0.15 per cent C, the heats were recarburized with pig iron and then blocked with 0.40 per cent Mn and 0.20 per cent Si at about 3100 F and about 5 to 8 min before tap. The balance of the Mn and Si, judged by the melter to be needed, was added 2 min before tapping. Heats were tapped at 3200 F with a 0.10 per cent Al addition in the ladle. The ore heats were identical with the exception of the 3 per cent additions of iron ore made to the bath when nearly melted. The only slag addition was 0.7 per cent limestone added during melting. (Reduced to 0.5 per cent on oxygen heats.) Any changes in slag composition during the heat resulted either from reaction, dilution, or absorption of refractories. Concurrent slag and metal samples were taken at intervals throughout the heats. Carbons were determined by combustion. Si and Mn contents and the slag analyses were determined spectrographically.

A survey of Table 3 indicates the improved Mn recovery typical of oxygen heats in our plant. Based on the addition plus residual, recoveries of 65 to 80 per cent were generally experienced on oxygen heats and 55 to 70 per cent on ore heats. The chemical differences on the oxygen heats were higher slag MnO,

lower slag FeO, and higher Mn residual after oxidation. Another significant difference was the lower quantity of slag on oxygen heats. Slag weights on 16 oxygen heats ranged from 1.7 to 4.9 per cent of the metal charge weight for an average ratio of 3.4 per cent. A number of ore heats averaged 5.2 per cent ranging from 3.0 to 7.4 per cent.

Detailed calculations on oxygen Heat 1 and ore Heat 5 are given in an attempt to explain the differences encountered. Heat 1 finished with a 76 per cent recovery of the Mn addition plus residual. Final slag on the 4000-lb heat weighed 128 lb for a 3.2 per cent slag to metal ratio. After melting with a 0.5 per cent limestone addition and reaching a temperature of 2900 F, the metal charge had undergone essentially no change in Si and C from the theoretical charge analysis. Mn had been decreased from 0.70 to 0.58 per cent. At this point the slag analyzed 18.3 per cent MnO and 23.2 per cent CaO and MgO. For such high concentrations of these constituents from only 0.12 per cent loss of Mn and one-half per cent addition of limestone, the slag weight at this time must have been less than I per cent of the metal weight. Slag FeO was only 9.7 per cent, kept low because of the high concentration of the other basic constituents and the Si and Mn content of the metal.

The injection of 200 cu ft of oxygen decreased the carbon from 0.49 to 0.33 per cent, the Si from 0.18 to 0.06 per cent, and the Mn from 0.58 to 0.25 per cent. Slag FeO increased from 9.7 to 16.7 per cent, MnO increased from 18.3 to 21.8 per cent, and CaO and MgO decreased from 23.2 to 11.2 per cent.

Since CaO and MgO were not involved in any reaction, such a reduction could only have resulted from the dilution effect of a slag volume increase to an approximate 2 per cent ratio. To increase a 1 per cent slag weight with 18.3 per cent MnO to a 2 per cent weight of slag with 21.8 per cent MnO would require 10.1 lb MnO for a 4000-lb charge. The 0.33 per cent Mn oxidized from the metal charge would produce 17.1 lb MnO. In view of the likelihood that the assumed 1 per cent ratio before oxygen is probably high and the 2 per cent after oxygen is probably low, more of the Mn could easily be accounted for. The quantity of slag continued to increase throughout the heat as indicated by the progressive decreases in concentration of CaO and MgO. Al₂O₃ likewise decreased from dilution until the aluminum addition to the ladle.

Two Mn additions totaling 0.85 per cent were made. Final Mn was 0.84 per cent leaving 0.26 per cent Mn lost from the addition. This would theoretically produce 13.4 lb of MnO in a heat of this size. The increase of a 2 per cent quantity of slag at 21.8 per cent MnO after the oxygen to a final slag ratio of 3.2 per cent at 23.8 per cent MnO actually accounts for 13.1 lb of MnO. All the 0.71 per cent Mn lost from the charge to the final content could have produced 36.6 per cent MnO. The final slag as collected and analyzed, actually contained 30.5 lb of Mn, accounting for 83 per cent of the total loss.

Heat 5 oxidized by a 4½ per cent addition of hematite ore when partially melted, experienced a 67 per cent Mn recovery and finished with the slag, as col-

COMPARATIVE CALCULATIONS: OXYGEN HEAT AND ORE HEAT

OXYGEN HEAT 1, TABLE 3, 4000-LB CHARGE Metal and Slag at Melt Down Assumed slag weight at Melt 1% or 40 lb Charge 0.70% - 0.58% = 0.12% Mn lost Melt slag 18.3% MnO 0.12% Mn × 4000 lb Charge = 4.8 lb Mn lost $4.8 \text{ lb} \times 71/55 = 6.2 \text{ lb MnO possible from the Mn lost}$ 40 lb \times 18.3% = 7.3 lb MnO actually in Melt down slag in Melting Metal and Slag Changes from Oxygen From dilution of nonreactive constituents must assume slag weight 0.58% Mn in Metal after Melt 0.25% Mn in Metal after Oxygen increased to approx. 2% or 80 lb after oxygen. 0.58% - 0.25% = 0.33% Mn lcst 80 lb × 21.8% MnO = 17.4 lb MnO after O₂ $0.33\% \times 4000 \text{ lb} = 13.2 \text{ lb Mn}$ 17.4 - 7.3 lb = 10.1 lb MnO actually added to slag by oxygen $13.2 \times 71/55 = 17.1$ lb MnO possible increase from treatment Mn oxidized Changes from Oxygen to Final Slag Final slag 128 lb \times 23.8% MnO = 30.5 lb MnO Addition Residual Final Lost 17.4 lb MnO in slag after Oxygen 0.85% + 0.25% - 0.84% = 0.26% Mn 0.26×4000 lb $\times 71/55 = 13.4$ lb MnO possible from 30.5 - 17.4 = 13.1 lb MnO actually added to slag from final Mn addition Mn lost from final addition Total Mn Lost vs Total MnO in Final Slag 128 lb final slag at 23.8% MnO Total Mn lost from charge and addition = 0.71% Mn $0.71\% \times 4000 \times 71/55 = 36.6$ lb MnO possible from 30.5 lb MnO actually in final slag total Mn lost ORE HEAT 5, TABLE 3, 4000-1b CHARGE Metal and Slag after Ore Down Charge 0.70% - 0.19% = 0.51% Mn lost Assume slag 3.5% = 140 lb $0.51\% \times 4000 \times 71/55 = 26.3$ lb MnO possible from Mn 140 lb imes 13.5% = 18.9 lb MnO actually in slag after lost from charge ore down Changes from Ore down to Final Addition Residual Final Final slag weight 5.1% or 204 lb at 18.9% Lost MnO = 38.6 lb MnO in final slag 1.00% Mn + 0.17% = 0.77% = 0.40% Mn $0.40\% \times 4000 \times 71/55 = 20.7$ lb MnO possible from Mn 38.6 lb - 18.9 lb = 19.7 lb MnO actually added to slag lost from addition from Mn addition Total Mn lost vs Total MnO in Final Slag Final slag 204 lb × 18.9% MnO = 38.6% MnO actually

lected, weighing 204 lb or 5.1 per cent of the charge weight. Before the boil, a considerable excess of FeO was held by the slag as indicated by its 33.3 per cent concentration. When sufficient temperature was reached, the carbon boil consumed the slag FeO down to 23.1 per cent.

Total Mn lost from charge and addition =0.92% 0.92% \times 4000 \times 71/55 = 47.5 lb MnO possible from total

Mn lost from charge and addition

This FeO content after the carbon removal is inclined to remain higher on ore heats than on oxygen heats in which the oxidizing agent reacts with the metal directly rather than being fed to the metal through the slag. After the carbon had been decreased to 0.22 per cent by the boil, the 0.05 per cent Si was comparable but the 0.17 per cent Mn residual was lower than on the oxygen heats. Slag MnO of 13.5 per cent and the CaO of 9.0 per cent were much lower than on the oxygen heat. These concentrations indicate a slag quantity of approximately 3.5 to 4.0 per cent, which is nearly twice that assumed at the end of the oxygen treatments. This greater volume accounts for the lower MnO concentration in the slag. In turn, lower slag MnO caused a lower Mn residual under conditions of equivalent oxidation.

As the heat progressed, the quantity of slag was increased by refractory consumption to a final ratio of 5.1 per cent as indicated by the progressive decreases in CaO concentration. The FeO decreased progress-

ively to a final concentration of 8.3 per cent and the MnO increased to a final of 18.9 per cent. As evident in the calculations, the step by step losses of Mn from the metal are fairly well accounted for in the concurrent MnO increases in the slag. The 38.6 lb of MnO in the final slag accounted for 81 per cent of the 47.5 lb of MnO chemically possible from the 0.92 per cent Mn lost from the charge and the additions.

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The several effects noted are all interrelated and simply emphasize the importance of the factors considered in the previous discussion. One obvious cause of better recovery on the oxygen heats is the smaller quantity of final slag requiring additional MnO. Another factor not indicated in the recovery, but reducing the necessary Mn addition is the 0.05 to 0.10 per cent higher Mn residual generally obtained. A third factor is a more favorable slag composition, higher in MnO and CaO and lower in FeO.

The higher residual and the slag composition are interrelated and both in turn are caused by the lower quantity of slag during oxidation of the charge. The usual ore addition of 4 per cent adds 1 per cent slag weight from the impurities, probably another 0.5 to 1 per cent weight from the FeO added to the slag, and probably another 0.5 to 1 per cent from SiO₂ fluxed from the refractories by the early concen-

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tration of FeO. With these sources of slag greatly reduced in the use of oxygen, equivalent Mn oxidation produces a much higher concentration of MnO, and equivalent lime additions produce higher concentration of CaO (unless the addition is reduced). Higher MnO and CaO contents in the slag tend to reach equilibrium with higher Mn residual in the metal and lower FeO in the slag. All three conditions decrease Mn loss necessary to reach slag equilibrium.

On oxygen Heat 2 with a 2.5 per cent slag weight ratio, an 0.80 per cent Mn addition produced a final of 0.81 per cent for a recovery of 101 per cent based on the addition only, or a true recovery of 80 per cent based on addition plus residual. A similar high Mn residual and slag MnO were experienced because of

the similarly reduced slag volume.

On Heat 3 more oxygen was introduced to obtain a very low carbon of 0.06 per cent in contrast to the higher "ore down carbons" of Heats 1 and 2. The Mn residual of 0.15 per cent is still high considering the extreme oxidation accomplished. Slag FeO of 14.5 per cent after oxygen was lower than on other heats removing less carbon. This is obviously due to the higher temperature of injection. An Mn recovery of 79 per cent was obtained in spite of the extreme oxidation. The low quantity of slag of 2.2 per cent made possible the good recovery.

On oxygen Heat 4 the quantity of slag was no lower than on comparable ore heats and the Mn recovery of 67 per cent was no better than on ore heats. Because of slag formed from excessive patching sand, the slag ratio must have been $2\frac{1}{2}$ to 3 per cent before introduction of the oxygen. After lowering the carbon from 0.42 to 0.23 per cent by the oxygen, the slag CaO content of 7.2 per cent was equal or lower than on ore heats and the slag MnO of 15.7 per cent was almost average for ore heats. The slag FeO of 26.3 per cent likewise was comparable to ore heats. The Mn residual of 0.17 per cent was consequently similar to heats using ore. From this it appears that one basic reason for the differences encountered between the use of oxygen and the use of ore is the difference in slag quantity developed. If this difference is minimized by some additional source of slag, chemistry and Mn recovery becomes more nearly equal. Likewise it seems reasonable that the difference would increase with higher Mn charges and decrease with low Mn charges.

From all indications, the few heats reported to have experienced excessive Mn losses from the use of oxygen were badly over oxidized by an excessive amount of oxygen introduced at a low temperature.

The use of oxygen led to the following conclusions. Equivalent decarburization with oxygen resulted in improved Mn recovery over the use of ore. Lower quantities of slag result which essentially explains the improvement. The higher Mn residual retained and a more favorable slag composition, are both caused by a lower volume of slag at the time of oxidation.

Summary

A study of concurrent changes in metal and slag composition on a variety of production and experimental heats led to certain conclusions that can be applied to obtain better Mn recovery and more consistent control. The results may be summarized in the following explanation of manganese loss in the acid furnace.

In both the acid and basic furnace most of the Mn that leaves the metal can be accounted for in the increased MnO of the slag. The reaction between metal and slag seems to follow the pattern of an interface type reaction and depends upon diffusion of the reactants within the two immiscible liquids. The manganese in the metal and the MnO of the slag seem inclined to seek an equilibrium ratio determined largely by the iron oxide activity of the slag-metal system. Both the equilibrium ratio and the oxide activity seem greatly affected by the basicity of the slag.

In the basic slag, the FeO is almost completely consumed in the Mn reaction but causes much less Mn loss and less increase of slag MnO because of its much lower original concentration. Under an acid slag the reaction is influenced by the FeO of the system, but a more dominant factor seems to be a demand for increased basicity and MnO content in the slag with a Mn increase in the metal. In the acid furnace consumption of slag FeO stops with considerable FeO left in the slag, but Mn loss continues if necessary to establish this acid-base balance.

When sufficient FeO is present in the slag, both the FeO equilibrium and the Mn-MnO balance in the metal and slag may be satisfied by the same oxidation reaction:

$$[Mn] + (FeO) = (MnO) + [Fe]$$

On the other hand, if sufficient slag FeO is not available to supply the MnO increase, the Mn reacts with the SiO₂ excess in the slag. As this reaction produces enough MnO for slag equilibrium, Si is reduced into the metal from the slag according to the following reaction:

$$[Mn] + (SiO_2) = (MnO) + [Si]$$

This reaction seems more predominant on high Mn steels and/or low FeO slags where there is a greater excess of the basic element in the metal and the acid oxide in the slag. In obedience to the laws of chemical equilibrium, the reaction from left to right is naturally promoted by increased concentration of either or both reactants on the left. In like manner, the reaction has been restrained and manganese recovery improved by increasing the concentration of either or both the products on the right, the MnO of the slag and the Si of the metal.

The final slag MnO content required for equilibrium seems to increase in an approximate straight line relationship with the final Mn of the steel. Oxides of other bases like CaO, MgO, etc., seem capable of substituting for MnO in the equilibrium slag requirements thus improving Mn recovery when added early enough.

As a scrap charge is oxidized, Mn oxidized from the metal contributes MnO to the slag until equilibrium is established between the residual Mn of the steel and the MnO of the slag at the prevailing temperature and oxide activity of the system. This equilibrium is affected by all the factors discussed. When the final addition of Mn is made near the end of the

heat, the equilibrium point is suddenly shifted to a higher Mn level. In an effort to secure necessary slag MnO, the Mn reacts first with the FeO then with the SiO_2 until the slag FeO reaches an equilibrium with the deoxidizing elements in the steel, and sufficient MnO has been produced to approach equilibrium with the declining Mn in the steel.

Increased temperature increases the total reaction, the Mn loss, and the final MnO of the slag. This effect of temperature was found more pronounced on acid than on basic heats.

An increased quantity of slag requires an increased amount of MnO for equilibrium satisfaction and thereby increases Mn loss. Unusually large slag volumes in the acid furnace generally cause the dilution of slag MnO and indirectly increase Mn loss still further through a less favorable slag composition. The substitution of oxygen for ore tends to reduce slag quantity and improve Mn recovery.

All previous theorizing has concerned the reaction inclination and the theoretical equilibrium point. In actual practice it seems highly improbable that complete equilibrium is ever reached. The extent to which this balance point is approached depends on a number of mechanical factors such as degree of agitation, fluidity, and time.

One method of improving Mn recovery is to reduce mechanically the opportunities for the Mn consuming reaction to proceed. Some such methods include intentionally developing a sluggish viscous slag, adding all Mn in the ladle, and removing the slag from contact with the metal. All of these have operational disadvantages which lessen the assurance of uniform

high temperature metal for castings.

Another approach to improved recovery is first to understand the reaction and equilibrium requirements, and then try more nearly to satisfy such requirements by slag adjustments. Complete recovery has not been accomplished but improvements have been made. Manganese oxide additions to the slag as well as second slags lower in FeO and higher in MnO and/or (CaO + MgO) have successfully improved Mn recovery on a production basis. Improvement in Mn control in the acid electric furnace involves control of or allowance for all the several factors influencing the reaction.

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DISCUSSION

Chairman: L. H. HAHN, Sivyer Steel Castings Co., Chicago. Co-Chairman: J. K. McBroom, Stainless Foundry & Engineering Co., Milwaukee, Wis.

H. H. JOHNSON (Written Discussion): The author is to be congratulated on making a very excellent contribution, both of theoretical background material and of practical considerations, to our knowledge of this important phase of the melting operation.

It is obvious that there are several factors that influence manganese recovery and that it is extremely difficult to isolate these factors so that the effect of each one can be measured. When we examine a slag cake for appearance or measure a slag viscosity, we are really integrating the effects of all the variables that are involved.

The author's attempts to measure the effect of these factors by considering only one variable at a time over a period of a few heats is indeed commendable as an approach to this problem, but the question can be raised as to whether or not some of his conclusions are justified because of the few number of heats involved.

For example, in connection with the consideration of the high manganese-oxide slags, the author concludes that "Recovery of Mn has been improved by increasing the slag MnO either by manganese oxide additions to the original slag or with MnO as the principal basic constituent of second slag mixtures." The author bases this conclusion on observations from six heats.

If the data from 26 heats, presented in Table 1 of the paper, are considered an attempt to correlate manganese recovery with the MnO content of the slag before the manganese addition results in a random distribution of observations and practically zero correlation. With an MnO content of 15 to 20 per cent and the same range of recovery holds for slag containing from 5 to 10 per cent MnO. A similar distribution was secured when data from our practice were examined.

We would interpret these data as meaning that the MnO content of the slag is not an independent factor. Certainly we do not feel that the erratic results of the six heats chosen by the author to substantiate his case are very convincing proof, especially when the results from other heats are considered. Similar criticism could be suggested about the data upon which other

conclusions are based.

Because there is such a random scatter of results between the Mn recovery and MnO content of the slag we would further examine the data contained in Table 1 to see if some other relationship could be obtained. Since both the SiO_3 and the MnO are products of the deoxidizing reactions as indicated in the paper, they should both act in the same direction so far as manganese recovery is concerned. The percentages of SiO2 and MnO present in the slags were therefore added and plotted against manganese recovery (Fig. 2) to see what degree of correlation existed. It seemed surprising to us that, with a few exceptions, the sum of the SiOg-MnO approached a constant value of about 70 per cent although the amount of each constituent present covers a range of 15 per cent. What trend there is points to an increase in recovery of manganese with increasing SiO₂ and MnO, as the author has indicated. This would seem to indicate that the reactions producing the SiO2 and the MnO try to reach a condition of equilibrium.

A fair degree of correlation exists between the FeO content of the slag and the recovery of manganese, as indicated in Fig. 3 and it is in the direction that would be expected from the

reading of the paper.

A practical application of such findings as the author has presented is in constant use in our shop where a chart, such as

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is shown in Fig. 4, is supplied the melters for calculating the size of the manganese additions. The slag viscosity (using the ½ in. bore viscosimeter) is used as a measure of the chemical composition of the slag. A different chart is supplied for each grade of steel, depending on its manganese content, and cognizance is taken of the amount of FeO present by noting the level to which the carbon has been reduced. The manganese recovery is thus obtained by reading the recovery to be expected at a given carbon level for a given slag viscosity. This assumes that other conditions, such as temperature, method of manganese addition, etc., will be found to be much as the author indicates.

In the interests of producing high quality steel we would question the advisability of following the author's suggestion of intentionally developing a sluggish viscous slag, and adding all the manganese in the ladle. This may be conducive to manganese recovery but is not conducive to producing good steel, as was pointed out a year or two ago by the author in this paper on effect of some melting variables on the tensile properties of acid steel.

J. W. JUPPENLATZ (Written Discussion).² Mr. Carter has presented a detailed and comprehensive review of manganese recovery in acid electric steelmaking. This is a subject that has received considerable study in the past and deserves further attention by furnace melters for more effective controls. The features of manganese control as favored by Mr. Carter are substantially correct. If some repetition is permitted, some of the more important factors of manganese control may be summarized as:

- Degree of oxidation, that is available FeO, of bath and slag, also carbon content of the bath.
- 2. Slag volume.
- 3. Condition of lining and temperature.
- 4. Manganese percentage of the metal.
- Acid-basic balance of slag, replacing some of the MnO with CaO and MgO.

These fundamental factors are considered most important for manganese control. Highest manganese recoveries may not be conducive of producing the best quality steels. Furnace practices will vary, but a total manganese recovery of about 70 to 75 per cent for a 0.25C-0.65 Mn grade appears to be optimum with the writer's practice. Higher recoveries are found when the bath is under oxidized, or when silicon content is increased by reduction of silica with very viscous slags with high temperatures. Low manganese recoveries are expected when reverse conditions prevail, that is, over oxidization, abundance of available FeO, thin, bulky, watery slags and so on.

Manganese is in the writer's opinion, a mild deoxidizer as well as a flux, and responds in a critical manner according to furnace practices being employed. Some grades of acid steel contain 1.50 or 2.00 per cent of manganese as an alloying element. Control of manganese with these higher contents are increasingly difficult, since even after pouring a well made heat into the ladle, there is a loss of manganese from the beginning of the heat to the end that is significant. Better and more consistent manganese recoveries are obtainable with basic electric practices with manganese alloy grades, and from an economical viewpoint should be employed, since the bulk of manganese ores are imported. Mr. Carter's Table 2 on basic electric heats supports this conclusion, showing manganese recoveries of 80 to 96 per cent.

MR. CARTER (Author's Written Reply to Messrs. Johnson and Juppenlatz): Mr. Johnson's remarks are appreciated. It is

admittedly difficult to isolate variables where so many are involved; and conclusions should be drawn with caution. However, the conclusions were not based on the few typical heats shown in the table. Control difficulties on a number of production heats instigated this study. Of over 100 heats with complete analytical data less than one-third were included in the paper and these were chosen to represent typical variations and deliberate extremes. The major conclusions reached were applied on a good number of production heats. As pointed out in the paper the high MnO double slag practice was applied on over 40 heats producing (in our plant) increased Mn recovery and improved consistency without sacrificing physical quality. This conclusion was not based solely on the six heats shown in the table as suggested.

The effect of MnO is further discredited on the basis of a graph of recovery vs MnO produced by Mr. Johnson from all 26 heats in Table 1. Since these heats in Table 1 represent deliberate extremes of several variables on several types of steel, nothing correlates very well with anything else because of the number of interrelated variables involved. Secondly, it was emphasized in the paper that poor correlation was found with slag MnO alone. In Fig. 5, it was found necessary to include CaO and MgO with MnO. The sum of these three constituents seem to represent essentially the effective basicity which did seem to influence the slag changes necessary for equilibrium.

We agree with Mr. Johnson that on ordinary foundry grades of steels, Mn recovery correlates more closely with slag FeO than any other single variable. The first factor discussed in the paper was slag FeO. In our plant Mn additions are adjusted principally on the basis of slag FeO as determined by slag appearance and fluidity. It was not our purpose in this paper to discard or lose sight of the progress made in this respect but rather to go further and explore some of the less common conditions under which our present knowledge was obviously inadequate.

Mr. Johnson's study of MnO + SiO₂ is interesting. Since both of these are products of the oxidation reaction while FeO is a reactant it would be expected that their sum would be an inverse measure of FeO to some extent. However, the other slag constituents cannot be ignored. Exceptions to this relationship are found in higher Mn steels in which SiO₂ may be reduced and MnO increased without much change in their sum but a considerable loss of Mn from the bath. It is my opinion that Mn and Si supplement each other in deoxidation until a deoxidation equilibrium has been satisfied. Then they begin to compete with each other until an acid-base balance has been obtained that is compatible with the final chemistry of the steel.

It was not intended in the paper to recommend a viscous slag or complete ladle additions to improve Mn recovery. These were mentioned as alternatives that have been reportedly used. A few such heats were run to complete the academic picture and prove their undesirability from the standpoint of uniformity and physical quality. In any attempt to improve alloy recovery and control, the effect on physical quality must be considered. Previous research has indicated the advisability of maintaining a moderately fluid and oxidizing slag for optimum physical quality. Such a practice is not favorable to the best Mn recovery. However, higher MnO slags seem to give the best Mn recovery for the same fluidity and degree of oxidation.

Mr. Juppenlatz's confirming remarks are appreciated. We agree, as he pointed out, that the Mn recoveries possible from the basic furnace cannot be obtained from the acid furnace. On high Mn steels the cost of the additional alloy might justify the cost of basic refractories. The few basic heats were included in the paper to bring out these points.

² Chief Metallurgist, Lebanon Steel Foundry, Lebanon, Pa.

TREATMENT OF BOND CLAYS FOR FOUNDRY SAND

By
A. E. Pavlish*

PART I

Treatment by Base-Exchange Reactions

BINDING PROPERTIES OF VARIOUS CLAYS used in bonding foundry sands vary widely and are influenced by numerous addition materials. The purpose of the investigation reported in this paper was to modify certain properties of two clays marketed for use in synthetic molding sands. One clay was a Mississippi bentonite and the other clay was a Lawrence plastic fireclay.

The present paper reports the results of study of the effect of base-exchange reactions in the clays under consideration. A considerable amount of fundamental work on the changes in the properties of clays that can be effected by base-exchange reactions has been done,1-8 and the importance of the base-exchange capacity of bond clays used in synthetic molding sands has been pointed out.4 The phenomenon of base exchange is the replacement of bases or positive ions in or on the clay particle by other positive ions. Thus, if one were to contact a certain clay with common salt (sodium chloride) it would result in the removal of some of the sodium from the solution. Contacting this sodium clay with calcium ions would result in the removal of some of the calcium from the solution, and the clay would lose some sodium. This base-exchange property is important because the bonding and plastic properties of the clay are dependent on the specific ion adsorbed.

Experimental

Standard A.F.S. test specimens were prepared from mixtures of Mississippi bentonite or Lawrence clay, sand, and water which had been mixed for 5 min in an 18-in. Simpson mixer. The permeability and green strength were determined immediately by standard A.F.S. methods. Specimens for dry-strength measurements were dried for 2 hr at 225 F in a drying oven equipped with a fan for circulation of the hot air, and

were cooled to room temperature in a desiccator over calcium chloride. Dry-strength measurements also were made in accordance with the standard procedures recommended by the American Foundrymen's Society.

The total base-exchange capacity of the two clays under study was determined by treatment with ammonium acetate solution at pH= 7.0 and distillation of the ammonium in the resultant clay.⁵ The results for the two clays were as follows:

| Clay | Base-Exchange Capacity, Milliequivalents/100 grams |
|-----------------------|---|
| Mississippi bentonite | 66.4 |
| Lawrence | 11.6 |

The first step in preparing these clays for investigation of the effect of base-exchange reactions was to convert them to hydrogen clays. This was done by treating the clays with sufficient excess of hydrochloric acid to replace all of the exchangeable bases present and to yield a clay slip 0.05 N in HCl. Specifically, the clay and acid were reacted in a porcelain ball mill for 10 hr, the liquid removed by filtering, and the clay residue washed twice with distilled water, the slip being mixed for 10 hr during each wash.

Aliquot portions of the hydrogen clay slip were removed for base-exchange reaction. The excess liquid was filtered off, an aqueous solution containing the desired cation was added, and the replacement allowed to proceed for about 20 to 200 hr with occasional mixing. In nearly all cases, 125 per cent of the theoretical amount of the desired cation necessary for the base-exchange reaction was added to the hydrogen clay for conversion to the desired form. Details of the amounts of reactants used are given in Table 1. At the end of the reaction period, the clay slip was filtered and the residue dried at 220 F. The dry clay then was disintegrated in a ball mill until it had approximately the same screen analysis as the as-received clay.

Slaked raw clays were prepared for use as controls in the tests. These clays were prepared by blunging the raw clays with approximately the same proportions of distilled water as were used in preparing the exchanged clays and for approximately the same length of time. After the final filtration, the slaked clays were Co

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TABLE 1-DATA ON PREPARATION OF CLAYS BY BASE-EXCHANGE REACTIONS1

| 1 | Reager | nt Used | | | Cent of ical Base- | |
|--|-------------------------------------|------------------------|-----------------------|-------------------------|-----------------------|----------------------|
| | | Amount, Mississippi | Per Cent ² | Exchange Mississippi | | |
| Type of Clay | Kind | Bentonite Clay | Lawrence Clay | Bentonite Clay | Lawrence Clay | Reacting Time, Hr |
| Sodium | NaOH | 3.34 | 0.58 | 125 | 125 | 65-67 |
| Sodium | Na ₂ CO ₂ | 4.46 | 0.66 | 125 | 125 | 22-24 |
| Sodium | Na _s SiO _s | 5.02 | 0.90 | 125 | 125 | 22-24 |
| Sodium | (NaPO ₂) ₆ | 8.46 | 1.48 | 125 | 125 | 22-24 |
| Sodium | NaF | 3.49 | 0.60 | 125 | 125 | 44-100 |
| Lithium | Li ₂ CO ₂ | 3.09 | 0.54 | 125 | 125 | 52-72 |
| Calcium | Ca (OH) ₂ | 3.09 | 0.54 | 125 | 125 | 60-96 |
| Calcium - | CaCl ₂ | 4.57 | 0.82 | 125 | 125 | 31-100 |
| Calcium | CaSO ₄ | 5.66 | 0.99 | 125 | 125 | 31-100 |
| Calcium | Ca (N0 ₈) ₂ | 4.23 | 0.74 | 125 | 125 | 31-100 |
| Molybdenum | MoO _a | 2.00 | 0.35 | 125 | 125 | 44-166 |
| Copper | CuO | 3.30 | 0.59 | 125 | 125 | 42-190 |
| Zinc | ZnO | 3.49 | 0.59 | 125 | 125 | 431/2-190 |
| Ammonium | NH,OH | 140 (of NHa) | 0.25 (of NHa) | 125 | 125 | 641/2-215 |
| Aniline | Aniline | 7.74 | 1.35 | 125 | 125 | 21-24 |
| Thiourea | Thiourea | 3.17 | 0.55 | 125 | 125 | 21-24 |
| Cinchonine | Cinchonine | 10.79 | 4.09 | 55 | 55 | 31-100 |
| Xylidene | Xylidene | 9.90 | 1.80 | 125 | 125 | 24 |
| n-Butylamine | n-Butylamine | 6.00 | 1.10 | 125 | 125 | 24 |
| Monoethanolamine | Monoethanolamine | 5.10 | 0.90 | 125 | 125 | 24 |
| Pyridine | Pyridine | 6.50 | 1.10 | 125 | 125 | 24 |
| Laurylamine | Laurylamine | 15.20 | 1.60 | 75 | 75 | 24 |
| Triethanolamine | Triethanolamine | 12.20 | 2.20 | 125 | 125 | 24 |
| β-Naphthylamine | β-Naphthylamine | 11.80 | 0.80 | 50 | 50 | 25-33 |
| Urea | Urea | 4.90 | 0.90 | 125 | 125 | 33 |
| Triethyl sulphonium hydroxide | Triethyl sulphonium hydroxide | 4.30 | 0.80 | 50 | 50 | 33 |
| Benzylamine | Benzylamine | 3.50 | 0.60 | 50 | 50 | 40-45 |
| | Trimethyl sulphonium hydroxide | 1.50 | 0.30 | 25 | 25 | 40-45 |
| Lauryl triethyl ammonium hydroxide | Lauryl triethyl ammonium hydroxide | 4.00 | 0.80 | 25 | 25 | 40-45 |
| Tribenzyl methyl ammonium hydroxide | Tribenzyl methyl ammonium hydroxide | 4.20 | 0.70 | 20 | 20 | 40-45 |

Hydrogen clays were used as the reacting materials in these base-exchange reactions. The hydrogen clays were prepared by the reaction of raw Mississippi bentonite clay with 2.6 per cent by weight of HCl or the reaction of raw Lawrence clay with 0.63 per cent by weight of HCl.

^a Based on the weight of dry hydrogen clay.

dried and disintegrated in the same manner as the exchanged clays. By these precautions, clays slaked to the same extent and with approximately the same amounts of soluble salts removed were obtained.

Mechanical Properties of Sands Bonded With Clays Treated by Base-Exchange Reactions—The effect of base-exchange reactions with the bond clay on the green strength, dry strength, permeability, and workability of sands bonded by the clays was determined. In all cases, 3000-gram batches were used, and the amounts of Mississippi bentonite and Lawrence clays used in the batch were 4 and 10 per cent of the total batch, respectively. Distilled water was used to temper the batches; 2.5 per cent of water was used with the Mississippi bentonite clays and 3.5 per cent of water was used with the Lawrence clays. The results reported in the body of this paper are presented on a percentage increase or percentage decrease basis compared to results with untreated clay to facilitate comparison of the data. Actual values obtained are reported in the tables of this paper.

Mississippi Bentonite Clays — Dry compressive strength of clay-bonded sand was lowered materially when the sand was bonded with clay modified by inorganic or organic base exchange. Organic base ex-

change of the clay produced a greater decrease in the dry compressive strength than inorganic base exchange. The green compressive strength was increased appreciably when sand was bonded with clays prepared by inorganic base-exchange reactions. Organic clays, with the exception of the urea, trimethyl sulphonium hydroxide, and thiourea clays, however, lowered materially the green compressive strength of the claybonded sands. Slaking the clay with water increased the green compressive strength and decreased the dry compressive strength of the clay-bonded sand. The change, however, was not so great, generally, as in the case of the clays prepared by base-exchange reactions. These effects are illustrated in Fig. 1 and Table 2.

Lawrence Clays—Mechanical properties of sands bonded with Lawrence clays prepared by base-exchange reactions are shown in Fig. 2 and Table 2. A substantial increase in the green compressive strength of the clay-bonded sand was effected by the hydrogen, calcium, copper, and molybdenum clays. No material change in the green compressive strength resulted when sand was bonded with zinc, ammonium, lithium, or sodium Lawrence clays. With the exception of the sodium, lithium, and calcium clays, which increased the dry compressive strength, the use of clays

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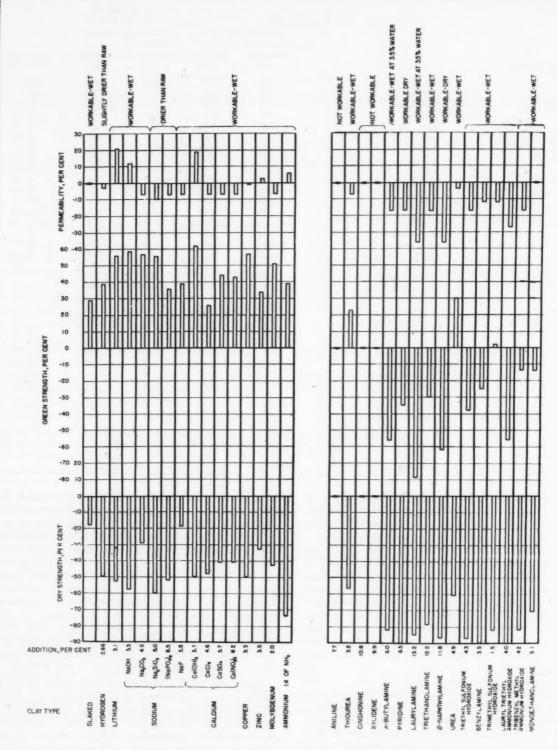


Fig. 1-Mechanical properties of sand bonded with Mississippi bentonite clays prepared by base exchange.

TABLE 2-MECHANICAL PROPERTIES OF SAND BONDED WITH CLAYS PREPARED BY BASE EXCHANGE

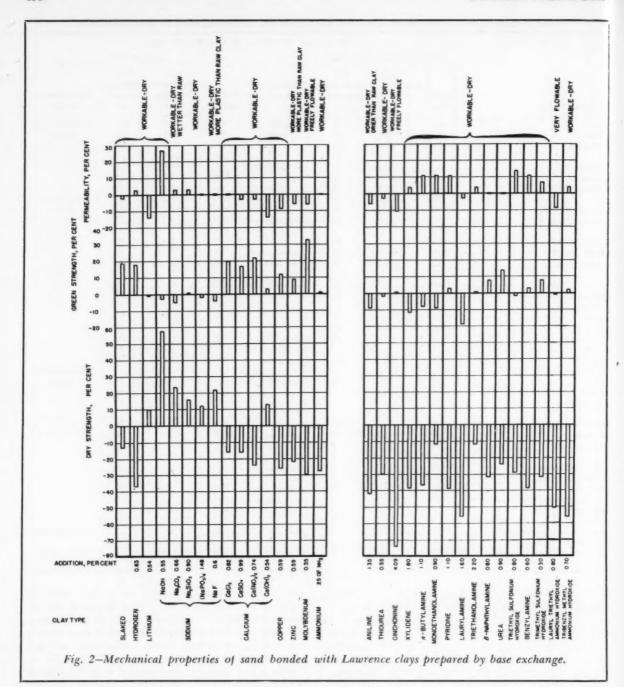
| | 361 | t!! D. | | ical Properties | Ja Cha | , | | |
|---|-------------------|--|--|------------------------------|-------------------|-------|--|-----------------------|
| | Miss | issippi Bento | nite Clay | | | Law | rence Clay | |
| Form of Clay | Perme- ability | Green Compressive Strength, psi | Dry Compressive Strength, psi | Workability of Mix | Perme- ability | | Dry Compressive Strength, psi | Workability of Mix |
| Raw | 167 | 9.0 | 50 | Workable-wet | 92 | - 9.3 | 60 | Workable-dry |
| Slaked | 165 | 11.6 | 41 | Ditto | 90 | 11.1 | 52 | Ditto |
| Hydrogen | 173 | 12.0 | 24 | 10 | 95 | 11.0 | 38 | ** |
| Raw | 181 | 11.4 | 36 | ** | 105 | 10.1 | 31 | ** |
| Lithium | 219 | 17.8 | 17 | Workable-dry | 90 | 10.0 | 34 | ** |
| Sodium (NaOH) | 202 | 18.1 | 15 | Workable-wet | 134 | 9.8 | 49 | ** |
| Calcium [CA (OH) ₂] | 215 | 18.5 | 18 | Ditto | 90 | 10.4 | 35 | ** |
| Raw | 229 | 9.9 | 42 | Workable-wet | 105 | 9.6 | 50 | Workable-dry |
| Sodium (Na ₂ CO ₂) | 212 | 15.5 | 30 | Ditto | 108 | 9.1 | 62 | Ditto |
| Sodium (Na ₈ SiO ₂) | 205 | 15.4 | 17 | Drier than raw | 108 | 9.7 | 58 | " |
| Sodium [(NaPO ₈) _n] | 212 | 13.5 | 20 | Ditto | 105 | 9.4 | 56 | |
| Sodium (NaF) | 212 | 13.8 | 34 | Workable-wet | 105 | 9.2 | 61 | ** |
| Calcium (CaCl ₂) | 212 | 12.5 | 22 | Less sticky | 105 | 11.5 | 42 | ** |
| Calcium (Calcig) | 414 | 14.3 | 44 | than raw | 100 | 11.0 | 12 | ** |
| Calcium (CaSO ₄) | 212 | 14.3 | 25 | Workable-wet | 102 | 11.2 | 42 | ** |
| Calcium [Ca (NO _a) ₂] | 212 | 14.2 | 25 | Ditto | 102 | 11.7 | 38 | |
| Molybdenum | 212 | 14.9 | 24 | " | 99 | 12.8 | 35 | 49 |
| Copper | 226 | 15.5 | 21 | 99 | 96 | 10.8 | 37 | ** |
| Zinc | 235 | 13.3 | 28 | 20 | 99 | 10.5 | 39 | ** |
| Ammonium | 243 | 13.8 | | Freely flowable | 105 | 9.7 | 36 | ** |
| Aniline | | _ | _ | Not workable | 99 | 8.7 | 29 | ** |
| Thiourea | 212 | 12.2 | 18 | Workable-dry | 102 | 9.8 | 35 | ** |
| Cinchonine | 616 | | | | 93 | 9.7 | 13 | Workable-dry |
| | 300 | 10.5 | 38 | Not workable | 156 | 8.8 | 41 | |
| Raw | 300 | 10.5 | | Workable-wet | 183 | 7.7 | 25 | Ditto |
| Xylidene | 019 | 4.0 | - | Not workable | | | | |
| n-Butylamine | 253 | 4.6 | 7.0 | Workable-wet (3.5% water) | 173 | 8.2 | 27 | ** |
| Monoethanolamine | 300 | 9.1 | 11.0 | Workable-wet | 173 | 8.0 | 36 | ** |
| Pyridene | 253 | 6.8 | 4.0 | Workable-dry | 173 | 9.1 | 25 | ** |
| Laurylamine | 193 | 2.2 | 5.0 | Workable-wet (3.5% water) | 151 | 7.1 | 18 | |
| Friethanolamine | 253 | 7.4 | 8.0 | Workable-wet | 183 | 8.9 | 36 | 99 |
| 3-Naphthylamine | 193 | 4.0 | 6.0 | Workable-dry | 156 | 9.4 | 28 | ** |
| Urea | 287 | 13.7 | 15.0 | Workable-dry | 156 | 10.0 | 31 | 40 |
| Triethyl sulphonium hydroxide | 253 | 6.5 | 5.0 | Workable-wet | 178 | 8.6 | 29 | 9.5 |
| Benzylamine | 264 | 7.9 | 4.0 | Ditto | 173 | 9.1 | 25 | 315 |
| rimethyl sulphonium hydroxide | 264 | 10.3 | 9.0 | 12 | 167 | 9.5 | 26 | ** |
| auryl triethyl ammonium hydroxide | 219 | 4.9 | 4.0 | Workable-dry | 142 | 8.7 | 20 | Very flowable |
| Tribenzyl methyl ammonium hydroxide | 253 | 9.0 | 9.0 | Workable-wet | 163 | 9.0 | 18 | Workable-dry |

treated by inorganic base-exchange reactions decreased materially the dry compressive strength of clay-bonded sands. Clays prepared by organic base-exchange reactions resulted in lower compressive strengths than with raw clay. Only the urea, trimethyl sulphonium hydroxide, and β -naphthylamine clays produced an appreciable increase in the green compressive strength.

Effect of Varying Water Content on Sand Bonded With Clays Prepared by Base-Exchange Reactions—Hydrogen, lithium, sodium, calcium, and raw clays were used as bonds for sand and the effect of various amounts of water on the mechanical properties of sand bonded with these clays was determined. The results are shown in Fig. 3 and in Table 3. The sand was bonded with 4 per cent of the Mississippi bentonite clays or 10 per cent of the Lawrence clays in these tests.

Mississippi Bentonite Clays—With the exception of the lowest water content used, 1.5 per cent, the clays prepared by base-exchange reactions had higher green compressive strengths than raw clay. The green compressive strength of the sand was greater with the lithium, calcium, and sodium clays than with the hydrogen clay at all of the water contents investigated. The sodium, calcium, and lithium clays were equally effective in promoting green compressive strength when the water content was 2.0 or 2.5 per cent. At higher water contents, sand bonded with the lithium clay had higher green compressive strength. The dry compressive strength of sand bonded with the exchanged clay was lower at all water contents than sand bonded with the raw clay.

Lawrence Clays—Sand bonded with the sodium clay had higher dry compressive strength than sand bonded with raw clay at all water contents. The green compressive strength of sand bonded with the exchanged clays was about the same or higher than that obtained with the raw clay. At the higher water contents, the sodium, lithium, and hydrogen clays effected a more marked increase in the green compressive strength than the calcium clay.



Discussion

Various inorganic and organic clays were prepared by base-exchange reactions which might be expected to have widely different properties. In general, the various clays did not differ in their effect on the mechanical properties of clay-bonded sands as greatly as might be expected. The anion of the salt, used to supply the cation being exchanged, influenced the properties of the clay as has been contended previously. The properties of the clay having the greater baseexchange capacity (Mississippi bentonite clay) were altered to a greater extent by base exchange than the properties of the clay with the lower base-exchange capacity (Lawrence clay) as was suggested by Grim, Bray, and Bradley.4*

Simultaneous improvement in both the green and dry compressive strengths of sand bonded with either Mississippi bentonite or Lawrence clays was not effected by inorganic or organic base-exchange reactions. Specific treatments improved the dry or green strengths of Lawrence clay-bonded sand and increased the green strength of Mississippi bentonite clay-bonded

[•] See page 1.

TABLE 3-MECHANICAL PROPERTIES OF CLAY-BONDED SAND WITH VARIOUS CONTENTS

| | | Mississ | ippi Bento | nite Clay | | | | Lawrence | Clay | |
|-----------------|--------------------------------|-------------------|--|-----------|--------------------------|-------------------------------|-------------------|--|------------|----------------------|
| | M | fecha nica | l Propertie | s of Sand | | | Mech | anical Prop | perties of | Sand |
| Form of Clay | Water, Content, Per Cent | Perme- ability | Green Com- pressive Strength, psi | pressive | Workability of Mixture | Water Content, Per Cent | Perme- ability | Green Com- pressive Strength, psi | pressive | |
| Raw | 1.5 | 275 | 17.0 | 17 | Barely work- able-dry | 2.0 | 115 | 10.9 | 19 | Not workable —dry |
| Sodium | 1.5 | 212 | 14.0 | 8 | Not workable —dry | 2.0 | 122 | 12.7 | 23 | Ditto |
| Lithium | 1.5 | 243 | 11.7 | 9 | Ditto | 2.0 | 122 | 12.1 | 17 | 0.0 |
| Calcium | 1.5 | 243 | 12.7 | 9 | 99 | 2.0 | 128 | 10.8 | 21 | 0.0 |
| Hydrogen | 1.5 | 226 | 10.1 | 7 | 09 | 2.0 | 108 | 11.3 | 17 | 80 |
| Raw | 2.0 | 275 | 15.0 | 41 | Workable-wet | 3.0 | 156 | 12.0 | 41 | Not workable -dry |
| Sodium | 2.0 | 287 | 22.3 | 17 | Workable-dry | 3.0 | 156 | 11.8 | 47 | Workable-dry |
| Lithium | 2.0 | 313 | 22.8 | 17 | Ditto | 3.0 | 146 | 13.4 | 42 | Ditto |
| Calcium | 2.0 | 313 | 22.4 | 20 | ** | 3.0 | 146 | 13.4 | 43 | 00 |
| Hydrogen | 2.0 | 243 | 19.3 | 14 | Not workable -dry | 3.0 | 167 | 13.0 | 32 | Not workable —dry |
| Raw | 2.5 | 181 | 11.4 | 36 | Workable-wet | 3.5 | 110 | 10.1 | 31 | Workable-dry |
| Sodium | 2.5 | 202 | 18.1 | 15 | Workable-dry | 3.5 | 134 | 9.8 | 49 | Ditto |
| Lithium | 2.5 | 219 | 17.8 | 17 | Ditto | 3.5 | 90 | 10.0 | 34 | 99 |
| Calcium | 2.5 | 215 | 18.5 | 18 | Workable-wet | 3.5 | 90 | 10.4 | 35 | 88 |
| Raw | 3.0 | 235 | 9.2 | 57 | Workable-wet | 4.0 | 163 | 7.8 | 58 | Workable-we |
| Sodium | 3.0 | 243 | 12.6 | 24 | Ditto | 4.0 | 156 | 9.4 | 78 | Ditto |
| Lithium | 3.0 | 264 | 15.2 | 33 | 80 | 4.0 | 156 | 9.0 | 59 | 10 |
| Calcium | 3.0 | 264 | 14.5 | 32 | 24 | 4.0 | 151 | 8.8 | 53 | ** |
| Hydrogen | 3.0 | 253 | 15.2 | 22 | 99 | 4.0 | 151 | 8.6 | 40 | |
| Raw | 3.5 | 255 | 7.5 | 60 | Workable-wet | 4.5 | 151 | 6.8 | 72 | Not workable —wet |
| Sodium | 3.5 | 212 | 10.9 | 28 | Ditto | 4.5 | 151 | 8.0 | 87 | Workable-wei |
| Lithium | 3.5 | 219 | 12.4 | 36 | 0.0 | 4.5 | 151 | 9.5 | 69 | Ditto |
| Calcium | 3.5 | 219 | 11.0 | 31 | ** | 4.5 | 151 | 7.7 | 61 | ** |
| Hydrogen | 3.5 | 219 | 11.3 | 20 | ** | 4.5 | 151 | 9.0 | 48 | ** |
| Raw | 4.5 | 223 | 4.7 | 61 | Not workable -too wet | - | - | - | - | |
| Sodium | 4.5 | 235 | 8.8 | 30 | Workable-wet | - | - | - | _ | - |
| Lithium | 4.5 | 235 | 10.5 | 33 | Ditto | | _ | _ | _ | nem. |
| Calcium | 4.5 | 235 | 7.8 | 29 | 0.0 | _ | _ | _ | _ | _ |
| Hydrogen | 4.5 | 235 | 7.6 | 17 | Not workable -too wet | | _ | - | - | - |

sand, but no improvement in the dry strength of Mississippi bentonite clay-bonded sand was effected by any base-exchange reaction used.

The desirable mechanical properties of the two clays were practically destroyed by organic base-exchange reactions. With the exception of the urea and thiourea clays, all of the organic clays produced little or no increase, or a marked decrease in the green and dry compressive strengths of the clay-bonded sand.

PART II

Effect of Various Lignin Sulphonates on Mechanical Properties of Clay-Bonded Sand

This part of the paper describes some of the other studies which were carried out on the improvement of two clays marketed for use in synthetic molding sands. In Part I of this paper, work was described on the effect of base-exchange reactions on the mechanical properties of sand bonded with the clays.

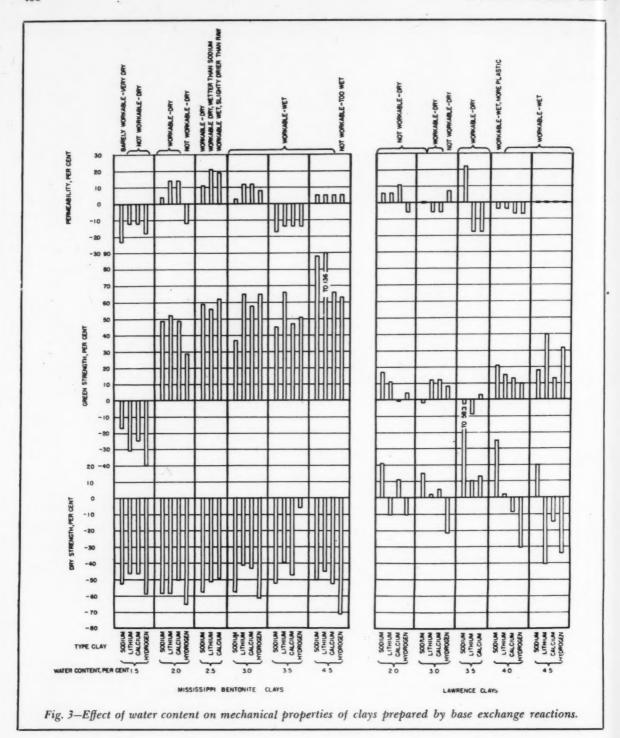
This part of the paper describes the results of an investigation conducted to determine the effectiveness of various lignin sulphonates in increasing the strength of clay-bonded sand. Previous work has shown that certain lignin binders were more effective than other

binders of supposedly the same type and it was desired to ascertain the reason for these differences. The usual commercial lignin extracts are by-products of the sulphite paper-making process, and are of the calcium lignin sulphonate type. Because of variations in the process from plant to plant, and differences in the wood pulp being treated, it might be expected that lignin binders produced by different companies will have different binding properties.

Experimental

Clays used in this study, Mississippi bentonite clay, and Lawrence plastic fireclay, were described in Part I. Methods of preparing and testing specimens in the present investigation also were the same as those described previously. The lignin binders were mixed with the clay and sand prior to adding the tempering water. As before, the results are presented in the body of the report on a percentage decrease or increase basis compared to results with untreated clays, and the actual values obtained are given in tables of this paper.

Effect of the addition of various lignin binders in amounts of 1 to 12.5 per cent by weight of the clay on the mechanical properties of clay-bonded sand is shown



in Fig. 4 and in Table 4.

Mississippi Bentonite Clay—Most of the binders effected a fair increase in the green strength of sand bonded with Mississippi bentonite clay. Goulac or binderene caused a decrease in the green strength. The effect of the addition of lignin binders to the sand on the dry compressive strength was variable; some binders increased the dry strength and some decreased it.

The binders which resulted in an increase in the dry strength at certain amounts produced a lowering of the dry strength when added in other amounts. Addition of most of the binders had little or no effect on the permeability of the sand.

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Lawrence Clay-Addition of various lignin binders to sand bonded with Lawrence clay had little effect on the green compressive strength of the clay-bonded

TABLE 4-EFFECT OF ADDITION OF LIGNIN BINDERS TO CLAY-BONDED SAND ON MECHANICAL PROPERTIES

| Missis | sippi Bentoni | ite Clay | | | | | rence Clay | | 6 |
|--------------------------|---------------|----------|----------|----------------|--------------|---------|------------|------------|--------------|
| | | Gree | en Com- | Dry Com- | | | | - Dry Com- | |
| Addition | | | | pressive | | | pressive | pressive | |
| | Amount, | Perme- S | trength, | Strength, | Workability | Perme- | Strength, | Strength, | Workability |
| Kind | Per Cent | ability | psi | psi | of Mixture | ability | psi | psi | of Mixture |
| | | | | RAW CI | AVS | | | | |
| None | | 185 | 9.6 | 38 | Workable-wet | 90 | 10.1 | 34 | Workable-dr |
| None | 1 | 185 | 9.9 | 34 | Ditto | 90 | 10.9 | 63 | Ditto |
| Bindex | 3 | | | 32 | Ditto | 88 | 10.2 | 88 | " |
| Bindex | | 185 | 11.4 | - | 99 | 88 | 10.5 | 86 | ** |
| Bindex | 5 | 185 | 10.6 | 31 | | 00 | | 00 | |
| Bindex | 7.5 | 178 | 11.5 | 33 | | _ | - | _ | _ |
| Bindex | 12 | 178 | 11.2 | 40 | 99 | | _ | _ | _ |
| None ' | _ | 179 | 11.1 | 32 | 99 | - | | 40 | 147 |
| Plasticade | 1 | 178 | 11.8 | 26 | | 80 | 11.1 | 43 | Workable-dr |
| Plasticade | 3 | 178 | 12.4 | 41 | | 80 | 11.3 | 45 | Ditto |
| Plasticade | 5 | 178 | 13.1 | 41 | ** | 80 | 10.2 | 59 | n |
| Plasticade | 7.5 | 167 | 13.1 | 27 | 99. | _ | - | _ | _ |
| Binderene | 1 | 173 | 10.2 | 45 | 10 | 85 | 10.1 | 73 | Workable-dr |
| Binderene | 3 | 173 | 11.5 | 38 | | 75 | 9.6 | 93 | Ditto |
| Binderene | 5 | 173 | 10.2 | 28 | 99 | 75 | 9.4 | 133 | 200 |
| Binderene | 7.5 | 173 | 10.1 | 42 | 29 | _ | _ | | - |
| None | 710 | 178 | 10.4 | 33 | 23 | _ | | - | _ |
| Goulac | 1 | 167 | 11.2 | 29 | 90 | 75 | 10.3 | 69 | Workable-dr |
| | 3 | 173 | 10.2 | 26 | 99 | 88 | 9.6 | 69 | Ditto |
| Goulac | 5 | | | 30 | 99 | 88 | 8.9 | 129 | 12 |
| Goulac | - | 198 | 8.9 | | ** | 00 | 0.5 | 149 | |
| Goulac | 7.5 | 193 | 9.4 | 51 | ** | _ | | _ | _ |
| Goulac | 12 | 193 | 9.9 | 25 | 99 | | 0.0 | - | Westerlie de |
| Marathon Grade "C" | 1 | 173 | 11.3 | 25 | | 88 | 8.6 | 75 | Workable-dr |
| Marathon Grade "C" | 3 | 156 | 11.5 | 35 | | 80 | 9.6 | 73 | Ditto |
| Marathon Grade "C" | 5 | 156 | 12.2 | 32 | | 88 | 9.5 | 97 | - |
| Marathon Grade "C" | 7.5 | 163 | 12.0 | 27 | ** | - | _ | - | _ |
| Marathon Grade "C" | 12 | 163 | 13.1 | 28 | 1.0 | _ | _ | | _ |
| None | decemen. | 176 | 10.6 | 32 | 99 | _ | _ | - | _ |
| Marathon Grade "P" | 1 | 185 | 10.5 | 36 | 29 | 93 | 10.4 | 48 | Workable-dr |
| Marathon Grade "P" | 3 | 185 | 10.6 | 32 | 20 | 85 | 10.6 | 52 | Ditto |
| Marathon Grade "P" | 5 | 185 | 11.5 | 41 | Workable-wet | 85 | 10.7 | 73 | Workable-dr |
| Marathon Grade "P" | 7.5 | 185 | 11.8 | 31 | Ditto | _ | _ | _ | - |
| Marathan Grade "P" | 12 | 178 | 12.9 | 28 | ** | _ | _ | _ | _ |
| | 1 | 178 | 10.8 | 37 | 9.9 | 90 | 9.7 | 43 | Workable-dr |
| Marathon Grade "PG" | 3 | 185 | 13.0 | 42 | ** | 90 | 10.3 | 73 | Ditto |
| Marathon Grade "PG" | | | | | ** | 88 | 10.0 | 82 | " |
| Marathon Grade "PG" | 5 | 173 | 11.8 | 33 | 99 | 00 | 10.0 | 04 | |
| Marathon Grade "PG" | 7.5 | 167 | 13.0 | 29 | ** | - | _ | _ | |
| Marathon Grade "PG" | 12 | 167 | 12.5 | 33 | | | | | |
| None | - | 174 | 11.1 | 31 | | 115 | 11.0 | 41 | Workable-dr |
| Sodium lignin sulphonate | 2.5* | 167 | 11.7 | 39 | ** | 119 | 9.8 | 73 | Ditto |
| Ditto | 7.5 | 163 | 11.8 | 38 | ** | 119 | 9.3 | 62 | |
| 99 | 12.5 | 167 | 12.3 | 33 | ** | 119 | 8.8 | 100 | ** |
| None | _ | 173 | 10.5 | 36 | ** | 88 | 9.6 | 28 | ** |
| Ammonium lignin sulpho | nate 2.5 | 173 | 10.6 | 31 | ** | 82 | 9.5 | 63 | ** |
| Ditto | 7.5 | 167 | 12.1 | 29 | ** | 80 | 9.4 | 89 | ** |
| P | 12.5 | 163 | 12.8 | 22 | 29 | 80 | 8.2 | 100 | ** |
| None | 16,0 | 174 | 9.6 | 36 | | 84 | 9.9 | 32 | ** |
| None | nate 2.5 | 178 | 10.4 | 36 | 99 | 85 | 9.6 | 31 | |
| Magnesium lignin sulphor | | 178 | 10.4 | 37 | 22 | 77 | 9.1 | 74 | ** |
| Ditto | 7.5 | 167 | 10.5 | 34 | 99 | 80 | 9.7 | 90 | ** |
| | 12.5 | 107 | 10.9 | - | Crave | 00 | 3.1 | 30 | |
| Nama | | 212 | 14.4 | HYDROGEN 22 | Workable—wet | 115 | 9.7 | 40 | Workable-dr |
| None | 100 | | | | Workable—dry | 102 | 10.2 | 45 | Ditto |
| Marathon "C" | 10.0 | 212 | 15.8 | 17 | | 96 | 10.2 | 40 | " |
| Sodium lignin sulphonate | 10.0 | 218 | 16.2 | 15 | Ditto | | | | ** |
| Ammonium lignin sulphor | ate 10.0 | 218 | 15.8 | 16 | | 102 | 10.8 | 45 | |

Additions of sodium lignin sulphonate, ammonium lignin sulphonate, or magnesium lignin sulphonate to Mississippi bentonite clay
were made in amounts of 2.5, 7.5, and 12.5 per cent; additions of these materials to Lawrence clay were made in amounts of 1, 3,
and 5 per cent.

sand. All of the binders produced a marked increase in the dry strength, however. Binderene, goulac, magnesium lignin sulphonate, and ammonium lignin sulphonate were especially effective. Most of the binders produced a slight decrease in the permeability of the sand.

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Hydrogen Clays—Addition of lignin binders to sand bonded with hydrogen clays did not produce so great a change in the properties of the sand as did additions to sand bonded with raw clays. Green strength of sand bonded with hydrogen Mississippi bentonite clay was increased by the addition of lignin binders; the dry

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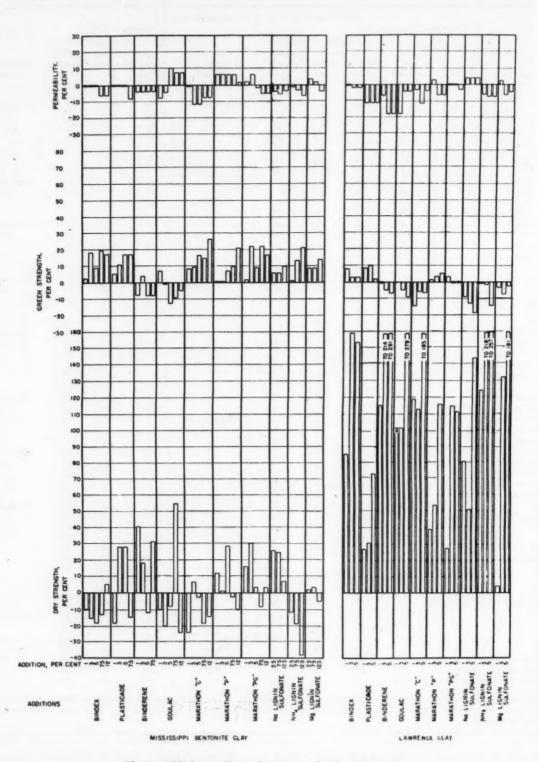


Fig. 4-Addition of lignin binders to clay bonded sand.

strength was decreased. Addition of lignin binders had little effect on the permeability. With hydrogen Lawrence clays, a slight increase in the green strength was brought about by the addition of lignin binders. and the dry strength was affected only slightly. The permeability of sand bonded with hydrogen Lawrence clay was decreased by addition of the binders.

Discussion

Various lignin binders obtained from different sources exhibited wide variation in their effects on the properties of clay-bonded sands. A partial chemical analysis of some of the binders studied, given in Table 5, showed that there was little difference in the inorganic constituents of binders which were vastly different in their binding abilities.

TABLE 5-ANALYSIS OF COMMERCIAL LIGNIN BINDERS

| | | er Cent | | | |
|---------------|------------------|---------|-----|-----------------|------------------|
| Binder | H ₂ O | Ca | Mg | SO ₈ | Total Sulphur |
| Bindex | 4.6 | 3.6 | 0.1 | 0.8 | 5.8 |
| Binderene | 4.3 | 3.2 | 1.2 | 0.8 | 5.4 |
| Goulac | 5.8 | 3.1 | 1.5 | 0.8 | 5.6 |
| Marathon "C" | 7.7 | 4.9 | 0.3 | 0.7 | 4.9 |
| Marathon "P" | 9.8 | 8.2 | 0.2 | 0.5 | 4.5 |
| Marathon "PG" | 9.2 | 7.8 | 0.2 | 0.6 | 4.1 |

Acknowledgment

Permission of Eastern Clay Products, Inc., to publish results of this investigation is gratefully acknowledged. The author wishes to thank C. R. Austin and J. D. Sullivan for their helpful encouragement and advice.

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DISCUSSION

Chairman: H. K. SALZBERG, The Borden Co., Bainbridge, N. Y. Co-Chairman: E. E. WOODLIFF, Foundry Sand Service Engineering Co., Detroit.

G. E. Dalbey: 1 Is there any practical effect to be expected from the addition of sodium hydroxide in small amounts to a molding sand, the sand being well mulled? Could you accomplish anything with it?

H. W. LOWNIE, JR.: 2 We know that the pH of molding-sand mixtures affects their properties. However, the addition of small amounts of sodium hydroxide, or other bases or acids, to molding sand in the muller is a type of treatment that is not within the scope of this paper. Undoubtedly, some effects on the properties of the sand would be obtained by such a treatment, but there is a question whether the effects would be large enough to be of practical importance.

CO-CHAIRMAN WOODLIFF: The reaction of the clay to treatment presented in the paper is a very definite reaction. The question comes to my mind, just what condition our clay is in at the time we want to treat it. We know that seacoal has produced an ash which may be caustic in nature and that we may have cereal or other compounds in the mixture which again changes the acidity or alkalinity of the final clay as it is in the molding sand. You are speaking of treatment in the sand as it is being used are you not?

MR. DALBEY: Yes.

Co-CHAIRMAN WOODLIFF: What do you think of the pH?

MR. LOWNIE: In trying to extend the information in this paper to treatment of molding sands in the muller, there is a very important point that should not be overlooked. This work was done to determine what the producers of clay could do to improve its properties. Because the treatments were to be made by the producers of the clay, long reaction times were not a serious disadvantage. Therefore, reaction times, as shown in Table 1, were from 22 to 190 hr. Also, treatment was done wet in a ball mill. These conditions promoted a high degree of reaction between the clay and the chemical additives. If the same additives were added to a molding sand as it was being tempered in a muller, it would be expected that reaction times would be much shorter and the reaction less complete. Therefore, to answer Mr. Dalbey's question about treatment in the muller, it would be necessary to use a different experimental technique than was used in this work.

CO-CHAIRMAN WOODLIFF: In the Transactions, A.F.S., vol. 57, pp. 210-221 (1949) there is an interesting paper entitled "Sand Properties vs pH" by Bradley Booth. In this paper the pH value is touched upon to a certain degree, although it is probably in different terminology, but it comes to the same end point. The control of the acidity of the sand in the foundry is of primary importance if we want to obtain the best out of bonding clays that we are using.

Bonding clays as yet are not a scarce item but in years to come we will have used up our best resources of clays. We do not have large quantities of western bentonite available even today. Top quality fireclays, too, are gradually being diminished.

I believe this work foretel's to the foundry industry the need for possibly recovering or improving our second grade natural clays. This paper means that to me and from that standpoint users of bond clay not only want the best available but you also want uniformity and perhaps by treatment the producer of clay will be able to give you a more uniform clay than he has heretofore.

H. J. WENDT: 8 In this base ion change with an increase of sodium ions, was it noted whether there was a corresponding increase in resilience or deformation?

MR. LOWNIE: With the Mississippi bentonite clays, treatment with sodium salts improved the green strength appreciably and improved the workability slightly. Although the paper contains no definite figures on resilience or deformation, it is reasonable to expect that a sand of appreciably higher green strength and better workability would also have higher resilience and deformation.

With the Lawrence plastic clays, treatments with sodium salts had little effect on green strength or workability. Therefore, these treatments would be expected to show little effect on resilience or deformation.

Co-CHAIRMAN WOODLIFF: In the second part of this paper there is an interesting bit of information to foundrymen who have sprayed molds with these sulphate products prior to drying. You have seen this very thing happen that is shown in the charts. It is necessary to build up the dry hardness of certain skin-dried or fired molds and the addition of this sulphate liquor, one of the various types, has been employed for a number of years. The use of different classes has been tried and has come to rather disastrous results in several foundries I happen to have been connected with. They were using southern bentonite or western bentonite, not covered in this paper, the dry strength was lost when we added a material to actually give greater hardness. But the old reliable use of fireclay as a binder proved in every case on the tests to be an increase in dry strength.

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² This paper and discussion were presented by H. L. Lownie, Jr., Assistant Supervisor, Battelle Memorial Institute, Columbus, Ohio.

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Recently I have worked on core sands. The problem was using core oil in a mixture containing one-half per cent addition of bentonite. This was an oil-sand core mixture. With 11/4 per cent cereal added to the mixture and 0.5 per cent bentonite, you naturally would expect a rather high green strength which he had in the muller. The sand in the mixer was very strong until the oil was added. Then the bond seemed to fall out of the whole sand and there was a much reduced strength after 1 or 2 min mulling. The green strength of sand had largely disappeared. The sand was very brittle and dead. The reaction was simply one of base change because as the problem developed we went into the testing of raw materials which went into the core oil and we discovered what material it was that was killing the green strength of core mixtures.

It is a very interesting thing to know that we can change the properties of sands, for we do get a reaction affecting the properties of binders by changing the acidity or the chemical pH value of the mixture.

Mr. Dalbey: The point just made indicates that there might be a possibility of a very rapid reaction and that was what I was somewhat interested in, in asking my first question.

Co-Chairman Woodliff: I was somewhat startled when I first read this paper, that it required anywhere near hours to

accomplish this base change. I, too, am of the same opinion that it happens not in hours, but in minutes, seconds as a matter of fact. It may be greatly accelerated when the clay is spread thin over sand grains more so that it would be enmass such as was dealt with in the work for the paper. Do you recall having accomplished it in any shorter time?

MR. LOWNIE: The work reported in this paper was designed to determine how much of an effect various treatments would have if the reactions were allowed to proceed to their limit. That is why these times were so long. The next step in the program would naturally be to pick out the most effective treatments and determine how much the reaction time could be reduced and still produce an appreciable effect. Investigation of shorter reaction times was not a part of this paper.

MR. Lownie (Author's Closure): On behalf of Mr. Pavlish, author of this paper, I wish to thank those who contributed to this discussion. We were glad to have had the chance to do this work to attempt to improve the quality of clays being used by foundrymen. We feel that it is very much to the credit of the producers of these clays that they are doing such work in attempts to improve the quality of clays so that foundrymen can be supplied with better clays of greater uniformity.

STANDARD DATA FOR BENCH COREMAKING

By H. R. Williams*

ABSTRACT

Certain principles which have been followed in applying standard data measurement to bench coremaking operations are presented in this paper.

Only those elements of the bench coremaking operation which may be considered as presenting a measurement problem are discussed. Sample curves are included to clarify the presentation of a method used in developing a "Fill and Ram" standard.

All illustrative figures used in this paper are taken from timestudies used to develop bench coremaking standards for a jobbing steel foundry. All time values are normalized to include a 331/3 per cent incentive allowance. Other necessary allowances are not included.

WHILE MOST FOUNDRIES DO HAVE some type of incentive coremaking operations, there are still too many that are without standard measurement and the many advantages such measurement affords. Better labor relations, the ability to estimate costs accurately, the control of methods, and the reduction of costs are only a few of the advantages which foundry management realizes when good standard measurement is applied to its shop operations.

Standards as applied to foundry operations have been discussed in papers previously presented to the American Foundrymen's Society. These papers have variously covered such subjects as "Timestudy Techniques," "Speed and Effort Rating," "Standard Data for Molding," "Standards for Grinding," etc. Because of the rather thorough coverage given such subjects, certain assumptions are being made in the preparation of this paper. These are, first, that the reader is familiar with accepted methods of timestudy and of speed and effort rating, second, that he is familiar with the difference between standard data measurement and direct timestudy, and third, that he realizes that standard data applies only as long as standard conditions are maintained.

Having made the above assumptions, it is proposed to discuss in this paper only the development of standard data for bench coremaking. In analyzing the illustrative figures and formulae included in this paper, it must be remembered that standard data is not

standard, it is specific. It is standard only for specific conditions of materials, for specific tools and equipment, for specific plant and departmental layout, etc., etc. For these obvious reasons, standards cannot be set down for an entire industry, craft, or operation, but they must be developed having all these variables in mind. In addition, the Standards' Engineer should have a broad background of coremaking "know-how" in order that he can analyze correctly the relative efficiency of the methods prevailing in his own shop. Naturally, standards must be developed for the best methods that can be applied to the specific conditions prevailing.

Since foundry Standards' Engineers will have little difficulty in measuring many of the coremaking operation elements, this paper will cover only those elements found to present problems in accurate work measurement. Some of these elements include, "Fill and Ram," "Tucking," "Striking," "Slicking," and "Drawing."

Coremaking Timestudies

On few other foundry operations is it so necessary that a timestudy man have a sound knowledge of the operation methods as it is when studying bench coremaking. Studies which include time for faulty methods can be so confusing as to make curve development for some element operations, such as "Fill and Ram" almost impossible. As an example, peening a core which could be adequately rammed by butt ramming would result in a "Fill and Ram" time that would not correlate with the base box characteristics.

Comprehensive standards for bench coremaking can be developed only if timestudies are extremely detailed. The total cycle for some of the small, simple bench cores may be 0.30 min or less. When we consider that the constant for picking up a coremaking tool may be as little as 0.03 min, it is easy to understand how incorrect analysis of method can result in a minimum error of 10 per cent in the core standard.

When taking a timestudy, every recognizable characteristic of observed method should be noted on the study. Sketches with notations of measurement should be made. Particularly valuable is the practice of securing a sample of each core studied. Since certain

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work features, such as some forms of tucking, are impossible to time as elements because of their inseparable combination with other work elements, the availability of sample cores will make much easier the analysis of studies.

It is necessary to take many more studies on coremaking than is necessary when developing standards for other foundry operations. This is true because of the infrequency with which certain work elements of coremaking occur. It is advisable, in order to avoid wasted effort, to begin curve development after relatively few studies have been taken so that the need for particular information can be determined and further studies can be selective in order to obtain needed data.

Because of "hidden" elements and because other coremaking elements may be too fast to time accurately with a decimal minute watch, many work values must be determined mathematically after an analysis is made of plotted points in their relation to a base curve.

Filling and Ramming

A correct analysis of filling and ramming requirements is probably the most important factor in the development of sound standards for bench coremaking. This is true for two reasons. First, since the "Fill and Ram" time is usually greater than the time for any other single element, an error in the standard value for "Fill and Ram" will result in a larger overall error than will the same percentage error in the standard allowance for any other element. Second, the work required to perform the "Fill and Ram" operation has a wider range of variation dependent on box characteristics than has the work involved in performing any other single work element.

Two methods are possible in the application of standard measurement to the "Fill and Ram" operation. Each method has its advantages and each is being used successfully in various foundry installations.

One method considers independently each element of filling and ramming and ties the standard allowance for each individual element to pertinent box characteristics. The other method treats filling and ramming in its entirety as a single unified operation and bases the "Fill and Ram" allowance on the relationship of corebox dimensions modified by certain other box characteristics.

The principle of "Individual Element" measurement results in greater accuracy and is better adapted to purposes of methods analysis and improvement. For these reasons, it is the measurement principle that

should be used by production foundries.

The method which treats filling and ramming in its entirety as a single unified work factor is advantageous for use by jobbing foundries for these reasons: First, extreme accuracy is of less importance on short runs because of the chance for balancing errors over the various jobs run during a shift. Second, since fewer factors need be considered in determining the "Fill and Ram" allowance, this principle of measurement requires less time to work out a job standard. Third, the use of this measurement principle tends to force the use of efficient methods by a coremaker since the

allowed time for filling and ramming any core is based on the assumption that the best possible methods will be used.

Development of "Fill and Ram" Standard

Since the principles of individual element measurement are generally understood, it will prove more valuable to discuss the development of a standard for combined filling and ramming based on box dimensions and on box characteristics.

That filling and ramming is related to the dimensions of a core box is easily appreciated. Taking an ordinary cigarette package as a representation of a core, we realize, that depending on draft, the core box could be made in either of three ways. The box might have a filling opening 21/8 x 23/4 in. and a depth of 7/8 in. It might have a filling opening of 7/8 x 23/4 in. and a depth of 21/8 in. Or, it might have a filling opening of 7/8 x 21/8 in. and a depth of 23/4 in. Obviously, the work required to fill and ram these three boxes would vary. The box with the smallest filling opening and the greatest depth would require the most work. Both in nature and in degree, the work necessary to fill and ram the boxes would be effected: although, the finished core from one box would differ little in appearance from the cores made in either of the other boxes.

Fill and Ram Classification

Use of this principle, which bases "Fill and Ram" allowance on core box dimensions, necessitates the use of two, three, or four box type classifications. The number of classifications to be set up depends on the degree of accuracy required and on the definiteness with which various classifications can be determined. When using this principle for determining the "Fill and Ram" allowance, faulty classification of box types is found to be the chief cause for error in the applied standard.

When three box classifications are used, their defini-

tions will approximate the following:

Class I—Boxes having no pockets, loose pieces, or other complications which effect the filling and ramming.

Class II—Boxes which are relatively regular in cavity contour but which have loose pieces, filling interruptions, and/or pockets which complicate the filling and ramming. Boxes which could be rammed for their full depth with a butting tool but which require the use of a peening tool to insure sharp corners on the core.

Class III—Boxes which have complex cavity contours, deep pockets, and/or many loose pieces to complicate the filling and ramming. Shallow boxes which have springy wires placed in the sand close to the top surface.

Obviously, there are many possibilities for refinement of such classification descriptions. The greater number of box features that are analyzed by actual counts and measurements, the more accurate will be the application of the "Fill and Ram" classifications.

Curve Development

The first step in developing a combined "Fill and Ram" standard is to determine the base curve for filling and ramming a Class I box. To accomplish this, the fill and ram normals from studies on Class I boxes are plotted against the actual volume of sand per box. A curve drawn through the lowest of the plotted points must represent, if sufficient studies are plotted, the easiest or minimum fill and ram requirements for boxes in this classification.

Figure 1 illustrates the type of summarized information which is used for the development of the base curve and from which further fill and ram information can be derived. Information in columns 1, 2, and 3 are entered on this form from individual time studies.

Figure 2 illustrates the determination of the base curve for filling and ramming Class I boxes. This curve is developed by plotting the normal times in column 2 against sand volumes entered in column 1. Only the lowest of the plotted points are considered when drawing the curve since it is the easiest type of "Fill and Ram" for boxes in this classification that is being determined. In the illustration, a curve with the formula 0.08 + 0.00294/cu in. was determined as being representative of the lowest of the plotted points.

When the base curve has been developed, the base curve reading for each box is entered in column 4 of Fig. 1.

The information listed in column 5 of Fig. 1 is next developed. The filling and ramming normal from the

FIG. 1-SUMMARY SHEET-FILL & RAM CLASS I BOXES

| Study Sand | (1) Cu. In. | (2) Normal | 1 | (3) Dimensions | | (4) Base | (5) Needed | (6) L-W | (7) D | (8) Total |
|------------|----------------|-----------------------|--------|-------------------|-------------|-------------|-----------------------|------------|----------|--------------|
| | Sand Volume | Minutes Fill & Ram | Length | Width | Width Depth | | Multiplying Factor | 2 | w | Ratio |
| 3 | 8 | 0.17 | 11/4 | 11/4 | 7 | 0.103 | 1.65 | 0.50 | 5.60 | 6.10 |
| 6 | 18 | 0.19 | 61/2 | 2 | 43/4 | 0.133 | 1.43 | 1.62 | 2.38 | 4.00 |
| 7 | 186 | 0.68 | 61/4 | 6 | 6 | 0.626 | 1.08 | 0.52 | 1.00 | 1.52 |
| 9 | 6 | 0.19 | 1 | 1 | 7 | 0.097 | 1.96 | 0.50 | 7.00 | 7.50 |
| 15 | 220 | 0.85 | 15 | 5 | 3 | 0.728 | 1.17 | 1.50 | 0.60 | 2.10 |
| 18 | 2 | 0.11 | 53/4 | 11/4 | 1/2 | 0.087 | 1.26 | 2.30 | 0.40 | 2.70 |
| 20 | 9 | 0.23 | 11/2 | 3/4 | 63/4 | 0.106 | 2.17 | 1.00 | 9.00 | 10.00 |
| 26 | 28 | 0.34 | 11/2 | 11/2 | 13 | 0.159 | 2.14 | 0.50 | 8.65 | 9.15 |
| 30 | 78 | 0.40 | 171/4 | 3 | 11/2 | 0.309 | 1.30 | 2.87 | 0.50 | 3.37 |
| 33 | 65 | 0.27 | 53/4 | 51/2 | 2 | 0.271 | 1.00 | 0.52 | 0.36 | 0.88 |
| 38 | 148 | 0.48 | 13 | 9 | 11/4 | 0.515 | 0.93 | 0.72 | 0.14 | 0.86 |
| 39 | 173 | 0.60 | 103/4 | 103/4 | 11/2 | 0.588 | 1.02 | 0.50 | 0.14 | 0.64 |
| 41 | 165 | 0.65 | 22 | 5 | 11/2 | 0.565 | 1.15 | 2.20 | 0.30 | 2.50 |
| 48 | 41/2 | 0.125 | 43/8 | 7/8 | 11/4 | 0.093 | 1.34 | 2.50 | 1.43 | 3.93 |
| 49 | 125 | 0.52 | 10 | 5 | 21/2 | 0.448 | 1.16 | 1.00 | 0.50 | 1.50 |
| 50 | 65 | 0.37 | 61/2 | 2 | 5 | 0.271 | 1.37 | 1.63 | 2.50 | 4.13 |
| 54 | 14 | 0.19 | 11/2 | 11/2 | 61/4 | 0.121 | 1.57 | 0.50 | 4.17 | 4.67 |

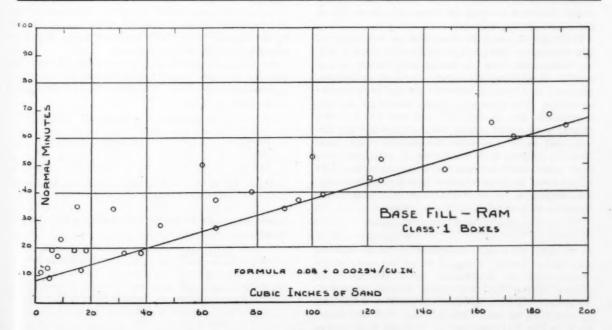
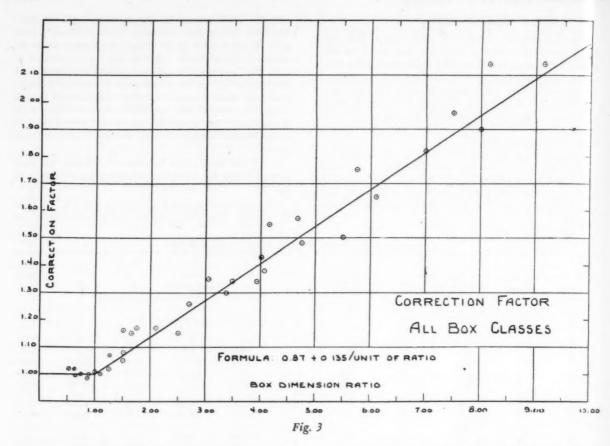


Fig. 2



timestudy is divided by the base curve reading for an equivalent sand volume in order to obtain an expression of relationship between the timestudy value and the base curve value. In column 5 are listed these relationships expressed in terms of the multiplying factors required to bring the base curve values up to the timestudy normals.

It is now necessary to determine an expression of box dimension relationships which will show a definite correlation with the multiplying factors in column 5. A mathematical expression of these relationships can be arrived at only by trial and error experimentation. The expression chosen should be one that is relatively easy to calculate and one which, when plotted graphically against required multiplying factors, will show reasonable correlation. The ratios listed in columns 6, 7, and 8, Fig. 1, were used after they were found to meet these requirements. The values listed in these columns show that this satisfactory expression of box dimension relationship was found to be a summation of two factors, i.e.,

$$\frac{\text{L (length)} \div \text{W (width)}}{2} + \frac{\text{D (depth)}}{\text{W (width)}}$$

In all cases, the longest dimension of the fill opening was used as length. Figure 3 shows the curve which results when the dimensional ratios, column 8, are plotted against the multiplying factors, column 5. This curve, the formula for which is 0.87 + 0.135/unit of ratio, can be used with the base "Fill and Ram" curve in Fig. 2 to calculate the "Fill and Ram" allowance for any Class 1 box.

The use of these curves in determining a total filling and ramming allowance can be illustrated by analyzing the dimensional characteristics of a core box used to make a core the size and shape of an ordinary cigarette package. Such a core would have a sand volume of approximately 5.1 cu in. The base filling and ramming allowance for 5.1 cu in., as calculated by the formula of the "Fill and Ram" curve, Fig. 2, is found to be 0.095 normal minutes.

As previously stated, such a core could be made in any one of three boxes depending on the draft requirements of the core. The three boxes would have dimensions as follows:

| | Length | Width | Depth |
|-------|--------|-------|-------|
| Box 1 | 23/4 | 21/8 | 7/8 |
| Box 2 | 23/4 | 7/8 | 21/8 |
| Box 3 | 21/6 | 7/9 | 23/4 |

Using $(L \div W) + D$ as the calculation of dimenwrith W

sional ratio, the following box ratios would be obtained:

Box 1
$$\frac{(23/4 \div 21/8)}{2} + \frac{7/8}{21/8} = 0.65 + 0.41 = 1.05$$

Box 2 $\frac{(23/4 \div 7/8)}{2} + \frac{21/8}{7/8} = 1.57 + 2.43 = 4.00$
Box 3 $\frac{(21/8 \div 7/8)}{2} + \frac{23/4}{7/8} = 1.21 + 3.14 = 4.35$

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ry d ig d

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From the formula 0.87 + 0.135/unit of ratio, for the "Correction Factor" curve, the multiplying factors for the above box ratios are determined. The following multiplying factors will be obtained:

| Box | 1 | 1.01 |
|-----|---|------|
| Box | 2 | 1.41 |
| Box | 3 | 1.46 |

The "Fill and Ram" standard allowance for each of these boxes would be obtained by multiplying the base curve value of 0.095 normal minutes by the indicated factor, as follows:

| Box | 1 | 1.01 | × | 0.095 | = | 0.096 | Normal | Minutes |
|-----|---|------|---|-------|---|-------|--------|---------|
| Box | 2 | 1.41 | × | 0.095 | = | 0.134 | Normal | Minutes |
| Roy | 9 | 1 46 | V | 0.005 | _ | 0 130 | Normal | Minutes |

After the proper personal, fatigue, and delay allowances are added to the base fill and ram curve formula, the values represented by this curve, and also the values represented by the "Correction Factor" curve should be set up in tabular form to facilitate the use of the information.

Having completed the development of the standard values for filling and ramming Class I boxes, it is now necessary to set up the standards for the other box classifications. In order that the multiplying factor values may be identical for all box classifications, the base curves for Class 2 and Class 3 boxes should be determined synthetically. This is done by utilizing the same type of summary form as was used for Class 1 boxes in Fig. 1. Information from the studies should be entered in columns 1, 2, and 3. The information required in columns 6, 7, and 8 is then computed. Values are next entered in column 5 from the "Correction Factor" curve developed for Class 1 boxes, Fig. 3. The study normals for "Fill and Ram" will be divided by the multiplying factor in column 5. The values resulting from this division will be entered in column 4. Column 4 values are next plotted against sand volume in column 1. The resulting curve will represent the base curve value for the box class being analyzed. As with the standards for Class I boxes, personal, fatigue, and delay allowances should be added and the information set up in tabular form for convenience of application.

Tucking

"Tucking," the packing or ramming of sand under a projecting overhang of the box or of a loose piece, is not compensated for in the "Fill and Ram" values. A separate standard must be developed to cover this operation. Obviously, there are many factors which govern the time requirement for tucking. Among them are the depth of the tuck, the length of the tuck, the accessibility through the fill opening of the box, and the size of the peening tool which can be used. Surprisingly accurate formulae, taking into account all of the above factors, have been developed. However, the most common type of tucking performed in bench coremaking is that done in an accessible spot by the use of fingers or small peening stick. In developing a curve for this operation, the normal minutes required for tucking should be divided by the length of the tuck to obtain a tucking value per inch of length. This value plotted against the depth of tuck will result in a curve the formula for which will approximate (0.0067 + 0.00195/in. of depth) per inch of length.

Sometimes both hands can be used simultaneously to perform the "Tucking" operation. This is common when tucking the outside circumference of sheave cores. The standard allowance for tucking simultaneously with both hands is one half the allowance given when only one hand or a peening tool is used.

Strike Off

This operation is that of removing the sand flush with the top of the core box after the ramming has been completed. A strike bar is usually used although, on very small cores, the operation is usually performed with a small trowel or slick.

It is most common to base the standard allowance for "Strike Off" on strike area. This is satisfactory although strike area is not the true measure of the work requirements of the operation. The time required to strike 30 sq in. of surface will not be identical for two cores, one of which has a surface 5 in. x 6 in. and the other which has a surface 10 in. x 3 in. The best correlation between normal time for "Strike Off" and surface characteristics will be obtained if strike off normal time, less the constants for picking up and disposing of the strike tool, is divided by the length of the strike and the obtained values per inch of length are plotted against the width of the strike.

Many core boxes will have projecting loose pieces or, in the case of booked cores, pins which will interrupt the strike-off operation. Since a loose piece, depending on its location in the sand surface, will present a varying number of sides to interrupt the strike, each interfering side should be counted and considered an interruption.

Since there is no way of timing elementally the additional work caused by a "Strike Interruption," the value must be determined synthetically. This determination can be made by subtracting the standard strike value from the timestudy normal time for striking off a surface containing projecting pins or loose pieces. The difference in these times plotted against the number of interruptions will produce a curve from which the standard allowance per "Strike Interruption" can be obtained.

Slicking

When there is a necessity for a smoother surface than that which can be obtained by striking, the struck surface is slicked down with a trowel or slick. This is the operation referred to as "Slicking." It is related to surface area and the standard is easily determined by plotting normal times for slicking against the area of the slicked surface. The determination of standard values for slick interruptions can be made in the same manner as that suggested for evaluating strike off interruptions.

Draw

Characteristics of the box which effect "Draw" time are fairly easy to analyze. Draft and thickness of sand sections are factors which effect the care required to make a good draw. The area of and in contact with the draw surface of the box is the governing factor of

| CUSTOMER PATTERN NUMBER | WIRES & RODS |
|--|---|
| DATE | |
| CORE CORES/BOX CORES/SET | No. Size Length Hor. Vert. Bends Fitted |
| STANDARD PER BOX STANDARD PER CORE | |
| BOX WOOD METAL SPLIT JOB CHANGE STANDARD | |
| L . FILL-RAM L X W X H | |
| VOLUME X X . | |
| WOAN (I'A WAN | |
| W AREA LXW * | Hooks No. Guage Top Bottom Side |
| FILL-RAN RATIO | Nails Horizontal No. Vertical No. |
| F R. FACTOR | SMALL Finish Nails |
| | Wire or Rod Cutting Allowance Chills Dimensions X X |
| Fill and Ram 1 2 3 4 Value . | Chills Dimensions A A |
| Draw 1 2 3 4 | TUCKING |
| Strike Strike interruptions | Characteristics Spots One Hand Two Hand |
| Re-Strike Re-Strike interruptions | Characteristics Spots One hand 1wo hand |
| Extra End Strike | |
| Slick Slick interruptions | |
| Rap for Draw Assemble Split Box | |
| C Clamp Ring Clamp Box Hooks | |
| Stick Vents Depth Number | Bed Under Overhang Size Bed Cavity Size |
| Push Vents Depth Number Located | Bed Cavity Size |
| Pipe Vents Size | Support Sand On Plate Inches Height |
| Trench Vents Length | Jolt, Knead, H.R. Fill Opening Depth |
| Scratch Vents Length | Plate or Relief Dryer On Core |
| Wax Vents Length Fitted Points | Need Help to Carry |
| Rod Through Box Vents Length | Sand Mixtures |
| Fill-Ram Small Hole Vol. Ratio Factor | |
| LOOSE PIECES SUM OF AVE. DIMENSIONS | |
| Depression Bar | OTHER OPERATIONS NOT LISTED |
| Side Rail, Flip Out | |
| Number 2 Draw | |
| Number 3 Draw | |
| Number 4 Draw Pick Draw | |
| | |
| Book Green Top Paste | |
| Patching Allowance | |
| Plate to Conveyor Boxes per Plate | SUB TOTAL (To Front of Card |
| BACK OF CARD SUB-TOTAL | OUD TOTAL (TO TIONS OF CALL |
| TOTAL M. MIN. PER BOX | |
| | Fin A Chariffaction and (Late) For the Carlot |
| Bench Coremaking | Fig. 4-Specification card. (Left)-Front of card ar |

(Right) Back of card.

"Draw" time when the "Care" characteristics are uni-

Three or more "Draw" classifications should be set up in accordance with various "Care" requirement descriptions. The curve for each classification of "Draw" should then be developed by plotting normal draw time against the following "Draw Factor" expression: $[L (length) + W (width)] \times D (depth).$

When three "Draw" classifications are used, the curves developed will have formulae approximating the following:

Class I Draw 0.043 + 0.00038/unit of draw factor Class II Draw 0.082 + 0.00057/unit of draw factor 0.100 + 0.00082/unit of draw factor Class III Draw

The above formulae cover only the actual draw. Such operation elements as "Rapping for Draw," "Transferring Core to Plate," "Plate on Prior to Roll,"
"Push Cores Up On Plate," and "Draw Tall Thin Core On End" are based on characteristics other than the "Draw Factor" and thus must be considered outside the actual draw.

When applying the "Draw" standard to split boxes, both halves of the box must be allowed for. It must be remembered that the dimension which may be box depth for the fill operation will be the length dimension when computing the "Draw Factor."

Rap for Draw

Although it might seem that "Rap For Draw" should be directly related to "Draw" and could be measured by the "Draw Factor," experimental plotting of "Rap" values will show that the best curve results when gross box volume is used as a base. Analysis of the operation will substantiate the use of the "Gross Volume" base. The formula of a "Rap For Draw" curve based on gross box volume will approximate: 0.040 + 0.00023/cu in. gross box volume.

Split Box Assembly

Assembly of a split box is another element which should be based on gross box volume. The curve obtained for this element will be one with a formula approximating: 0.025 + 0.00053/cu in. gross volume.

Loose Pieces

The drawing and resetting of a loose piece is an operation for which the time can be suitably correlated to the size of the loose piece. A figure of expression which is easy to calculate and which proves a good base is the sum of the loose piece dimensions. Totaling the inches of average length, width, and thickness is the method used for computing "Sum of Dimensions."

It is advisable to set up at least three classifications of Loose Piece draw based on the depth of draw, draft of the loose piece, and on the care required to prevent tearing of the core or of breaking thin sand sections or overhanging sand projections.

Other Operation Elements

There are a great number of operation elements which will not be discussed in this paper. As stated previously, most of these elements will be relatively easy to measure for determination of standard allowances. Nails, wires, and rods, various types of venting, and core plate allowance are a few of the other elements for which standards must be developed to be able to measure the work involved in making bench cores.

Specification Card

The sample "Specification Card," Fig. 4, is one used by a jobbing steel foundry. Both front and back of this 5-in. x 8-in. card are utilized in listing the most common elements. Some elements which occur infrequently are not listed. Such elements are "written in" when they are required to produce a core.

Conclusion

Bench coremaking standards developed according to the principles presented in this paper are being used successfully by many foundries. All installations have resulted in greater production, reduced costs, in standardization of methods, prices which afford equal earnings on all jobs, and in better cost estimating and control.

DISCUSSION

Chairman: E. G. TETZLAFF, Pelton Steel Casting Co., Milwaukee.

Co-Chairman: M. T. Sell, Sterling Foundry Co., Wellington, Ohio.

Member: I should like some information on classifying cores, take a sheave core for instance. Just as you brought out, you have only a small amount of sand but you give it a fill and ram classification after using your circumference as length, the width of the rammed surface as width, and the depth of the core as depth. In just what classification would such a sheave core fall?

g

MR. WILLIAMS: The question is what classification should be given a sheave core which has a relatively small amount of sand? This is not a simple box and yet it is not one that should fall in the most complicated class.

In the paper I have made some rough sketches of three classifications. The second classification covers boxes that have loose pieces. Loose pieces always complicate ramming to a certain extent and for that reason this sheave core would fall in the second classification.

Most sheave core boxes have a loose ring in the box under which sand must be tucked. This tucking must be handled as a separate time value. Any tucking, whether done with the fingers, a stick, or a peening tool, is not compensated for in the base fill and ram values. It is net fill and ram time—fill and ram time less tucking time—that is plotted on these curves.

MEMBER: Do you run into any complications on various types of sand once you have your figures established on a job? Suppose you change sands? Right now we are experimenting with different sands. I am making numerous timestudies on the ramming of cores. We are trying this new sand in ring cores,

probably 24 in. in diam and from 6 to 8 in. thick. We put a layer of sand in the bottom, probably 2 or 3 in., and then ram around toward the loose pieces with a rammer. This type of sand requires quite a bit of ramming, but there is a doubt in my mind whether the amount is not being exaggerated. I do not know. Do you run into such problems on various types of sand?

MR. WILLIAMS: I wish I could say no. Sand type is a definite factor in filling and ramming. However, I have found that the influence of sand mixture on filling and ramming is usually over exaggerated by the men in the core room.

There are certain sand mixes, those with practically no binder, used in certain small cores, which do require additional ramming allowances. Other sands may not effect the filling and ramming time but they do have a definite effect on the standard for the core because of its effect on the requirements for cleaning the core box. With certain sand mixes, a coremaker may be able to run a number of cores without using parting or cleaning the box. Another sand mix may necessitate parting or cleaning the box for each core made. Until cleaning requirements are considered, job standards will be incorrect.

I find that it is possible to set up simple factor methods to cover additional allowances for the various sand mixes used in

Getting back to the problem relative to the necessity of the amount of ramming done while you are taking a study, such job stuffing happens to all of us. I know of no other way to solve the problem but to insist, through the foreman, that a particular core be rammed according to your specifications. Then it is up to your technical people in the shop to tell you whether or not the core, rammed to those specifications, is satisfactory. If it is, set your standard on that basis and request the coremakers to operate according to the standard procedure you prescribe.

MEMBER: In other words, you may end up with two different standards if one sand is found to be more difficult to use. You would have to have a standard for that particular sand.

MR. WILLIAMS: That is right. I think you may be able to compensate for such other sand mixes by utilizing multiplying factors applied to your base fill and ram curve. This will eliminate the necessity of preparing voluminous tables of data.

E. C. Reid: 1 Have you at any time attempted to account for your variations in your posted figures? After you have posted your data from your time studies, you have drawn your line through the lower ones. Do you attempt to account, in any way, for the higher ones?

MR. WILLIAMS: On this volume curve (Fig. 2) the line is drawn through the lowest points because that curve must represent the very simplest type of filling and ramming you are going to find in your core room. We accept the fact that there is justification for the higher point. We account for the greater time requirements in those jobs represented by these higher points by utilizing this other curve (Fig. 3). Figure 3 correlates box characteristics with the multiplying factor required to compensate for the additional work involved in those jobs represented by these high points on the volume curve.

Mr. Rein: I did not quite understand the progressive steps of setting up the data.

Members: In your studies you did not take into account the weight of the core box or the weight of the sand. It is strictly a box volume ratio.

Mr. WILLIAMS: The volume that I am using is the actual sand volume of the box. Many shops actually weigh the core. The volume measurement is not the result of a multiplication of the greatest dimensions of the box. We try to ascertain actual sand volume.

The box weight is not considered in this set of standards. Perhaps it should be. I think that if I were setting up standards in a production shop I would take box weight into consideration. Certainly it is important from the standpoint of fatigue.

¹ Industrial Engineer, Ford Motor Co. of Canada Ltd., Windsor, Ontario, Canada.

USE OF PORTABLE AIR TOOLS IN FOUNDRY CLEANING ROOMS

By A. G. Ringer*

PRESENT HIGH LABOR RATES require that utmost care be used in selecting proper size and type of pneumatic chipping hammers and grinders for foundry cleaning rooms. It is only by use of most efficient tools that low production costs can be achieved and operator fatigue kept to a minimum.

Use of Chipping Hammers

Chipping hammers are used in the cleaning room to remove fins, gates, risers and to knock out cores. The following recommendations are based on production tests and experience which proved satisfactory.

Iron and steel foundries can handle most chipping problems with a Size 2 chipping hammer, while for the heavier work such as removal of gates or risers, a Size 3 is preferred. In malleable and nonferrous foundries, a Size 1 hammer will handle most work.

However, it should be remembered that most brasses and bronzes tend to work harden so that extremely light cuts are generally advisable to avoid stalling of the chisel in the cut.

The numerical reference to hammer size refers to the nominal length of the piston stroke in inches. Recent developments in chipping hammer design now make it possible to fit exactly the hammer to the work and to the operator by changing only one small part.

Figure 1 shows a cleaning room chipping operation where the castings are worked on hand trucks so that they can be moved without crane handling. Note that all operators are wearing gloves and goggles for hand and eye protection.

The same size hammers as used for chipping in general can be used for core removal on small and medium castings. The only additional equipment required are chisels of special shapes and lengths designed to reach inaccessible areas. On large castings, such as Diesel engine blocks, a heavier, more powerful hammer is required. For this purpose a pneumatic core breaker is recommended. The core breaker uses a retaining device for the chisel, and is especially designed for this work. Figure 2 shows a core breaker in use in a large foundry.



Fig. 1



Fig. 2

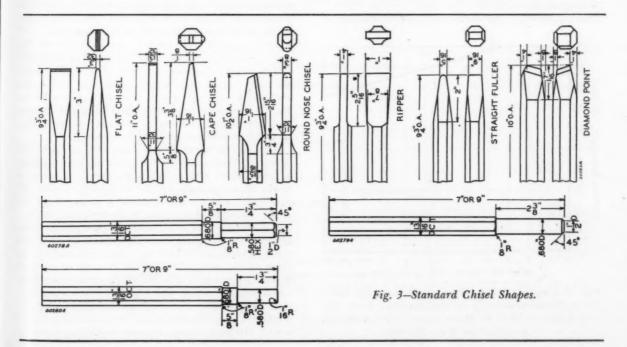
^{*} Ingersoll-Rand Company, New York.

180 to 200

200 to 230

TABLE 1

| | | | | A cha | rt for | use in | detern | nining | suitable | e hose | ar- ow. | | | | |
|--------------------------------------|-----------------|----------------|-----------------|-----------------|------------------|---------------------|-------------------|-----------------|-----------------|--------------------|-------------------|------------------|---------------------------------------|------------------|------------------|
| Air Flow Cubic Feet Per Minute | 10' of 14" Hose | 8' of 5's Hose | 16' of 96" Hose | 12½' of ½" Hose | IN' of 1/2" Hose | 30 of 34" Hone | 1235" of 34" Home | 25' of 94" Hose | 50' of 54" Hose | 80' of 1/2" Hose + | 80' of 1/2" Hose+ | 80' of 34" Hose+ | 80' of 34" Hose+ 123%' of 34" Hose | 10' of 14" Hose+ | 80' of 90" Hose+ |
| Per minute | | - | | | | Press Based on I | ure Drop- | Pounds | per Squar | e Inch | ure | | | | |
| 10 to 11 | 5.0 | 1 .9 | 1 | 1 | | | | | | 5.3 | :7 | 1.4 | | | 1 |
| 11 to 12 | 5.9 | 1.0 | - | | | | - | - | | 6.2 | .8 | 1.6 | | | |
| II to 13 | 6.8 | 1.2 | .4 | | - | 1 | | | - | 7.3 | .9 | 1.9 | | - | |
| 13 to 14 | 8.0 | 1.4 | .5 | - | | | | - | - | 8.4 | 1.1 | 2.2 | | | |
| 14 to 18 | 9.3 | 1.6 | .6 | | - | | | | | 9.8 | 1.3 | 2.5 | | | |
| 15 to 16 | 11.0 | 1.9 | .7 | | | - | - | | | 11.6 | 1.5 | 2.9 | | | |
| 16 to 18 | 14.0 | 2.4 | .8 | | | | | | | 15.0 | 1.9 | 3.5 | 1.7 | | |
| 18 to 20 | 19.6 | 3.0 | 1.0 | | | | | | | 21.4 | 2.4 | 4.5 | 3.0 | | |
| 20 to 25 | | 4.3 | 1.4 | .7 | 1.0 | 1.3 | | | | | 3.5 | 6.4 | 2.6 | 1.3 | |
| 25 to 30 | | 6.6 | 2.1 | 1.0 | 1.8 | 2.8 | | | | | 5.2 | 9.8 | 3.8 | 1.9 | |
| 30 to 35 | | 9.8 | 3.1 | 1.3 | 2.1 | 3.6 | | | | | 7.8 | 13.7 | 5.3 | 2.6 | |
| 35 to 40 | | 12.8 | 4.2 | 1.7 | 2.8 | 5.2 | | | | | 9.6 | 18.4 | 7.1 | 3.5 | |
| 40 to 80 | | 19.3 | 6.3 | 2.4 | 4.1 | 8.0 | | | | | 14.0 | | 10.4 | 5.2 | 1.8 |
| #0 to 60 | | | 9.6 | 3.7 | 6.3 | 12.2 | | | | | 21.8 | | 16.0 | 7.8 | 3.3 |
| 60 to 70 | | | 13.5 | .5.3 | 9.0 | 17.4 | .9 | 1.4 | 1.9 | | | | 22.8 | 11.1 | 8.0 |
| 70 to 80 | | | 18.7 | 7.1 | 12.4 | | 1.1 | 1.7 | 2.5 | | | | | 15.0 | 3.7 |
| 80 to 90 | | | 25.0 | 9.0 | 16.1 | | 1.4 | 2.2 | 3.2 | | | | | 19.8 | 4.6 |
| 90 to 100 | | | | 11.1 | | | 1.7 | 2.7 | 4.0 | | | | | | 5.8 |
| 100 to 130 | | | | | | | 2.3 | 3.5 | 5.6 | | | | | | 7.9 |
| 120 to 140 | | | | | | | 3.2 | 4.8 | 8.0 | | | | | | 11.3 |
| 140 to 160 | | | | | | | 4.3 | 6.6 | 11.0 | / | | | | | 15.5 |
| 160 to 180 | | | | | | | 8.6 | 8.7 | 15.2 | | | | | | 30.4 |
| | | - | - | - | | | | | | | | | | | - |



11.0

7.2

9.0

In this connection it should be pointed out that chipping hammers are used for this heavy work in far too many cleaning rooms. This may be due in some cases to operator resistance. Since long and heavy steels or chisels are necessary in circumstances such as shown in Fig. 2, it can readily be seen that a chipping hammer cannot deliver an effective blow at the point of the steel.

The selection of the proper type of chipping chisels is as important as the selection of the proper size of chipping hammer.

Figure 3 shows the standard shapes of chisels. Many modifications of these are made to suit the nature of the work. Figure 3 shows standard shank dimensions.

Chisels are frequently used until they are mere stubs; this is false economy. An examination of such chisels usually proves that the shank no longer approximates the standards shown in Fig. 3. The result is shortened piston bore and piston life for the chipping hammers as well as an increase in operator fatigue due to excessive vibration.

Chipping hammers are often taken for granted, that is, the assumption is made that as long as the hammer makes a noise it is doing its job. Figure 4 shows that this is not the case. In this graph, a new hammer was used to cut a weighed chip to establish the 100 per cent condition, and then similar weighed cuts were made with worn pistons in the new bore. If these same worn pistons were to be used in worn bores much lower efficiency would be found than this curve indicates. Therefore, it will be seen that it is easy to continue using hammers at 75 per cent and less of new tool power. At today's labor rates this can be costly.

Use of Pneumatic Grinders

Pneumatic grinders form another important group of portable tools which are used extensively in the cleaning room. These are of two general types, namely, horizontal and vertical.

The horizontal type uses straight wheels ranging in

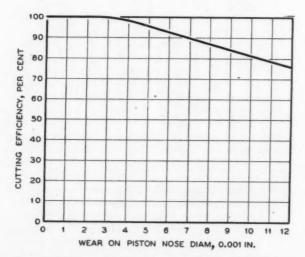


Fig. 4—Chipper Piston Wear vs Cutting Efficiency. Worn Pistons Operating in New Standard Bore. Cutting Efficiency Will Be Much Lower if These Pistons Are Used in Worn Barrel.



Fig. 5

size from small mandrel mounted points approximately 1 in. in diameter to the largest popular size of 8-in. diameter. These grinders are used primarily for grinding irregular, or curved surfaces, as shown in Fig. 5. Whenever the work permits, guards should be used.

Selection of the proper size of grinder ties in with the question of what size wheel is to be used. If the work demands an 8-in. wheel with 2-in. face, the largest or most powerful grinder will be required. For 6-in. wheels a tool of higher rpm is necessary, and at the same time since less horsepower is called for a slightly lighter portable grinder can be applied. In any case, the governed speed of the grinder should correspond to the rated rpm of the wheel to be used.

One popular application of the horizontal grinder is for grinding with cone-shaped wheels of $2\frac{1}{2}$ -in. or 3-in. diameter. Such tools are medium-weight grinders running at a free speed of 7200 or 9000 rpm, and should be equipped with a stub style arbor to prevent operators from applying 6-in. or 8-in. wheels which are not safe beyond 4500 and 6000 rpm. Cone wheel shapes work into filleted corners to advantage.

Vertical grinders are made in two sizes. The smaller one is generally governed to a free speed of 6000 rpm, and is suitable for 5-in. cup-type wheels. The larger size has a free speed of 4500 or 5000 rpm., and is used with a 6-in. cup type wheel. This type of grinder can also be used with flexible sanding pads and abrasive discs for surface dressing of castings requiring high surface finish such as machine tool castings.

The vertical type grinder with its complementary flared cup type wheel has the advantage of presenting a larger wheel area to the work. For this reason it works advantageously for the smoothing of gate and riser stubs, and for the removal of fins on large flat areas. This type of operation is illustrated in Fig. 6.

Both the vertical and horizontal type grinders have recently found application for removing gates and risers on medium-size iron and nonferrous castings too large to be conveniently carried to table-type cut-off wheels or band saws. For this job, a semi-flexible high-speed abrasive cut-off wheel is used. Where the location of gates and risers will permit the use of this method, a considerable reduction in handling time is noted. On iron castings, this way of nicking gates and

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Fig. 6

risers prior to break-off offers an advantage over the use of chipping hammers.

Since the top speed of air grinders is limited, it will be seen that a grinding wheel is below its best performance point after approximately 25 per cent reduction in diameter. For instance, to obtain the recommended surface speed of approximately 9000 surface feet per minute an 8-in. elastic bonded wheel may be run at 4500 rpm. When this wheel is reduced in diameter to 6-in. the rpm should be about 6000 rpm for best metal

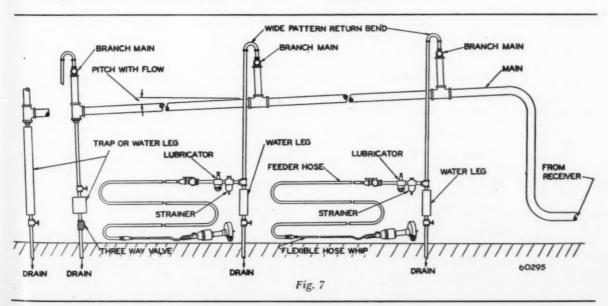
removal. If grinders of both speeds are available, the worn wheels can be put on the higher speed machines with a consequent gain in metal removal per unit of time. However, if this practice is followed, wheel application should be in the hands of a tool room attendant schooled to know what the safe speeds are to prevent the cleaning room laborer from applying slow-speed wheels to high rpm machines.

Air tools frequently fail to do an effective job due to low air pressure at the throttle. This is sometimes due to insufficient compressor capacity, but just as frequently results from improper hose fittings. A pressure drop from 90 to 70 psi results in a performance loss of over 20 per cent in a typical grinder.

Even where hose type nipples are used undersizes frequently appear. The importance of this detail is illustrated by the fact that a change from a ½-in. hose by ¼-in. pipe thread nipple having a ½-in. diameter hole to a ½-in. hose by ¼-in. pipe thread nipple having a ½-in. diameter hole results in an increase of 67 per cent in air passage area. Undersized air hoses cause corresponding friction loss as illustrated in Table 1, where pressure drops due to friction are shown for various air demands in cubic feet for several different hose combinations. Your tool and compressor suppliers should be consulted for a full analysis of your conditions.

Air pressure at the tool inlet can now be quickly determined by means of a pocket pressure gage equipped with a hypodermic-type needle which is inserted into the hose.

In Fig. 7 the best practice in air system piping is diagrammed as recommended by the Compressed Air Institute. This illustration lays stress on pitching air mains with the flow, taking down-comers off the top of the main by means of wide pattern return bends, the use of water legs and line lubrication of tools. The line lubricators illustrated have transparent bowls holding an oil supply sufficient for a week or more so that tool life is not dependent upon the disinterested laborer.



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DISCUSSION

Chairman: J. THOMSON, Continental Foundry & Machine Co., East Chicago, Ind.

Co-Chairman: H. W. Johnson, Wells Mfg. Co., Skokie, Ill. Chairman Thompson: Is there any merit to having a meter to measure the air consumption of a new tool and periodically

meter it to see if it is using more air?

MR. PURTON: Yes, there is but it will not give you the answer as to whether the tool should be replaced with a new one because of wear. At the point where you start to lose money in the actual production from the hammer, the air consumption is almost the same. You do not lose much air because of only 0.0005 in. of wear at the center of the piston. However we get a rattling of the piston in the barrel and loss of production.

CHAIRMAN THOMSON: In other words you would advise taking

the tool apart at intervals for inspection.

MR. BURTON: It is a good procedure to do that for another reason. The piston wears at the striking face to a cup shape where it strikes the chisel and when that is very sharply cut it starts to spawl your chisels and your repair man should take the piston out and carefully grind it square again.

Chairman Thomson: What recommendation do you have for

MR. BURTON: Your grinders should be checked periodically in the tool room for air consumption. That is important because that will give you your loss in power right away. They should be checked for power. This can be done by the use of a fan in the place of a wheel and the fan can be calibrated on a new grinder so that you can tell by the loss in rpm in the machine what the power of the grinder would be out on the floor.

FOUNDRY APPRENTICE TRAINING AT CATERPILLAR TRACTOR CO.

By

F. W. Shipley* and B. L. Bevis**

CATERPILLAR TRACTOR Co. has long recognized the value of training and for a considerable length of time has maintained extensive training programs throughout the plant for various lines of endeavor. Need for such extensive training programs has been borne out by the steady growth of the plant.

When the initial training programs were inaugurated in 1928, total employment in all operations at Peoria was approximately 4500. Today employment is approximately 23,000 and the total number of trainees enrolled in all the training programs on January 1, 1950 was 594. The training programs in effect as of this date are as follows:

APPRENTICE COURSES

| Machinist Apprentice | (4-year) |
|--------------------------|----------|
| Foundrymen Apprentice | (4-year) |
| Patternmaker Apprentice | |
| Wood and Metal | (4-year) |
| Electrician Apprentice | (4-year) |
| Millwright Apprentice | (4-year) |
| Pipefitter Apprentice | (4-year) |
| Heat Treater Apprentice | (3-year) |
| TRAINEE COURSES | , , , |
| Machine Shop Trainee | (2-year) |
| Welding Trainee | (2-year) |
| Business Trainee | (3-year) |
| Cooperative Trainee | (5-year) |
| College Graduate Trainee | (1-year) |
| Sheet Metal Trainee | (2-year) |

In this paper we are considering only the foundry training programs which as you can see includes foundrymen apprentices, patternmaking, both wood and metal, apprentices.

In order to maintain a constant flow of trained personnel for the foundry division, it is planned that eight foundrymen apprentices, two wood patternmaking apprentices and four metal patternmaking apprentices be started in the courses each year.

Applicants for these training programs must be high school graduates between the ages of 18 to 21 inclusive. They are selected by the training super-

visor according to their ability, personal qualification, test results and their scholastic record. These tests consist of mathematics, mechanical comprehension, personality, aptitude and dexterity. The final decision however, is based on the outcome of a personal interview with the applicant. He is shown through the foundry, various stations where he will work are pointed out and comprehensive explanations of the entire course are given to him.

After entering the course the apprentice is observed closely for the first six months. During this time the supervisor of training is able to better determine his personality and leadership qualities, his ability to work with others, and the amount of interest he actually shows in the particular course which he has entered. If sufficient undesirable qualities are found he is immediately removed from the course. We are most anxious to provide job satisfaction in all training programs as well as produce quality graduates and do not desire to waste our time upon those who do not possess the desired qualities which we are seeking.

We believe in the principle of learning by doing. The effort expended by every apprentice at each station is measured upon the same standards of measured day work that is applied to all other personnel in the foundry. He is not considered an extra employee in any sense, but must maintain production and his time is charged to the particular department in which he may be working.

Foundryman Apprentice Course

Rates of Pay—The term of apprenticeship is four years or 8000 hours. This time is divided into eight six-month or 1000-hour periods, rate increases being given at the completion of each period.

\$.94—Orientation Period .99—Second Period 1.04—Third Period 1.09—Fourth Period 1.15—Fifth Period 1.20—Sixth Period 1.25—Seventh Period 1.30—Eighth Period 1.41—Graduating Rate

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^{*} Foundry Manager and ** Supervisor Foundry and Pattern Shop Training, Caterpillar Tractor Co., Peoria, Ill.



Foundry apprentice making mold using loose pattern. He prepares, pours and shakes out his own molds.

Tools—Upon entering training, apprentices are issued a complete set of foundry tools. The sum of five dollars is deducted from his check each month until the tools are paid for. Upon graduation, he is reimbursed the full amount of these tools and they then become his permanent property.

Shop Training Schedule

In the shop each apprentice is under direct supervision of the foreman in charge of the department where he may be working. He receives help and instruction from the foreman in the department as well as from the training supervisor who constantly observes his progress and helps answer his questions.

The following is an outline of stations on which the apprentice gains training during his four years. These stations are listed in sequence of advancement from one job to another. From time to time, through gradual improvement it is deemed necessary to add or subtract various training stations. In the past it had been the practice to start the apprentice in the small core room. Now, however, the boy is started on the loose pattern molding station. This is also the final station in the course. We realize that he may be of little value on this station at first, because he knows absolutely nothing about it. However, we have found that it is an excellent place to develop interest. He begins to realize that he is producing something. He soon becomes familiar with the causes of scrap and realizes his own lack of knowledge. In other words, instead of the boy starting in the core room where he can see absolutely no value to the

work he is doing and purposes seem vague, he is able to see why cores are used, how patterns are used. what molding sand is for, why iron needs to be hot and observe those things which might cause a casting to be scrapped. He is working with something real and is able to see the result of his own efforts. When he later starts his work in the small core room he has at least a working knowledge of what he is doing and why it is necessary.

Molder (Loose Pattern) (Slip Flasks)—2 Months—The apprentice makes molds for jobbing production of small gray iron castings for tool room, experimental and non-current parts. He works with loose patterns, determines molding procedure and uses all types of molder's hand tools to ram reinforce and vent molds. He also sets and anchors various cores. He makes molds on a jolt-squeeze machine or by hand ramming on a bench. He prepares and pours his own molds. After pouring, he shakes out castings to analyze the effect of the gating, risering and molding procedure used. Needless to say much practical safety instruction is given at this station.

Coremaker (Small Cores)—2 Months—The apprentice makes a variety of cores ranging from the simplest chunk cores using a dump box, to more complex cores with boxes having unusual draft. Some require several loose pieces or may involve a knitting frame and wax venting. The cores are made either on the bench, or blower or small hand rollover machines. He will utilize all three methods while on this station.

Core Assembler (Small)—1 Month—This station covers inspection, cleaning, rubbing, mending, fitting and assembling all types of minor cores. Some will be paste-in, collar, body, barrel, or pin cores, usually those used frequently. The apprentice joins cores using paste, chaplets and wedges; checks assemblies with height, snap, thickness or caliper gages. He makes small hand adjustments as necessary, and smooths, muds and paints joints on assemblies.

Coremaker (Large)-2 Months-The apprentice



Foundry apprentice making simple pin core from split core box in small core room.

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makes a wide variety of cores on a power jolt rollover draw coremaking machine. These cores are quite large, requiring considerable hand ramming and drawing of loose pieces.

Core Assembler (Large)—2 Months—This work usually involves pasting or bolting together assemblies on a variety of larger sized cores, including Diesel cylinder blocks and transmission case cores. Much use is made of gages and jigs.

Molder (Machine) (Cope and Drag Patterns)—1 Month—The apprentice operates jolt-squeeze strip or jolt-squeeze rollover draw machines performing the mold ramming operations for a variety of castings such as clutch drums, housings, clutch plates, cylinder liners, manifolds, etc.

Coresetter (Cope and Drag Units)—1 Month—On this station, the apprentice sets a wide variety of cores ranging from small simple stopoff and pin cores weighing a few ounces up to large size complex assemblies. He carefully places cores in molds, seals, anchors, supports and spaces them with tins, or chaplets. He checks the alignment of cores with templet gages and makes necessary adjustments. He blows dirt from the mold and assists in the closing operations.

Mold Finisher (Tractor Slinger Unit)—2 Months—The apprentice performs all finishing operations on molds for castings such as Diesel cylinder blocks, transmission cases, covers, oil pans, fly-wheels and axle housing. He inspects molds for such defects as soft ramming and broken sections. He uses finisher's slicks and trowels to repair defects, if necessary reinforces corners and surfaces with nails. He also vents the mold to aid escaping gases.

Coresetter (Sand Slinger Unit)—2 Months—The apprentice begins his training as coresetter on the sand



Foundry apprentice performs finishing operations on large sandslinger molds as shown on this floor.



Foundry apprentice sets large cores and core assemblies as shown on this large conveyor unit.

slinger unit by working with a journeyman. He sets large cores, usually handled by an overhead crane. He assists in setting, anchoring, sealing and supporting cores and uses a large overall gage to check his work. During the last month on this training station, he works alone. The last week on this station is spent working on the various sand mixing units. Flow sheets are prepared to illustrate the mixing processes.

Sand Tester (Sand Laboratory)—2 Months—The apprentice performs repetitive tests to determine the properties of the molding and core sand mixtures. The properties tested for are strength, moisture content and permeability of sands from each unit in the foundry. In addition to these routine tests, samples of purchased sand and bonds are analyzed according to A.F.S. standards.

Molder (Loose Pattern)—4 Months—Same as first training station but using more complex patterns.

Molder (Jobbing Floor)—2 Months—The apprentice works with loose and split patterns. He determines parting, location of gates and risers, and general molding procedures for large gray iron castings for tool room, experimental and non-current parts. He mixes sand of various types as required, determining the type of sand, density of ramming, amount of reinforcement and venting required. He fits, assembles and sets complex cores and uses all types of molder's hand tools and methods for ramming and finishing.

Patternmaker (Wood)—2 Months—The foundry apprentice constructs, alters or repairs simple wood pattern equipment. He learns to use woodworking machines safely and receives instructions in the correct use of woodworking hand tools. The apprentice be-



Foundry apprentice preparing sample of molding sand for moisture test. He also tests for strength and permeability.

comes familiar with the steps in pattern construction and design.

Patternmaker (Metal)—2 Months—At a metal patternmaker's bench, the foundry apprentice assists a journeyman on a current job and does mixed bench practice such as: fitting driers, minor repair and first gatings. During this station, he receives instructions about the machine tools and hand tools the metal patternmaker uses.

Cleaning Room—1 Month—Here he gains experience in hand-chipping, grinding and the use of pneumatic tools. He operates a stationary and swing frame grinder in removing gates and risers from the casting. The apprentice also gets experience in shot blasting and checking castings in the scribe and grind layout fixtures.

Inspector (Rough Casting)—2 Months—The apprentice inspects quality of castings prior to delivery to the machine shop. During the first few weeks at this station he works with the regular inspector. Later, however, he is given full responsibility for this work.



Foundry apprentice assisting regular inspector in rough casting inspection.

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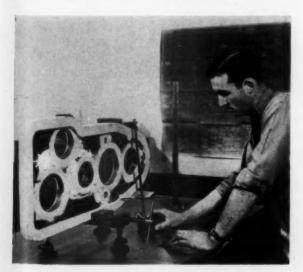
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Foundry apprentice pouring chill test sample, reading temperature and making alloy additions. Samples are poured from each ladle. Temperature readings are made each 15 min.

Layout (Rough Casting)—2 Months—Sample castings are set up on a surface plate and checked thoroughly as to drawing dimensions. This training further develops his blueprint reading ability.



Foundry apprentice laying out rough casting with regard to trueness to drawing dimensions.

Machine Shop—2 Months—In the Machine Shop the apprentice gains some experience in assembly, machine operation and heat treating.

Technical Helper (Metallurgical Laboratory)—2 Months—He collects and prepares samples and makes routine laboratory analyses of gray iron to ascertain its chemical composition and physical properties. The findings are checked with engineering standards and specifications. All analyses are made in accordance with an established procedure and his work is closely checked by the supervisor.

Cupola—3 Months—On this station the apprentice learns how to make alloy additions, check temperatures and pour chill tests and samples for the laboratory. He spends some time repairing the cupola and learning the methods used to reline and patch the cupola and all sizes of ladles. He also works on the charging floor to familiarize himself with the different charges and charging procedures.

Quality Control (Foundry and Machine Shop)—2 Months—The apprentice serves as liaison between the foundry and machine shop, aiding in elimination of scrap, improvement of casting quality and foundry processing. He analyzes practices of cleaning, molding or coremaking which may be changed to improve quality of castings. He proposes improvements by discussion with supervision in the departments concerned.

Quality Control (Foundry)—2 Months—The apprentice assists various foremen in scrap control. He checks castings for the usual foundry defects as they come from the shake-out.

Foundry Planning—2 Months—Timestudy (Foundry)—The apprentice observes elements of motion and machine functions, materials, tools and equipment preliminary to taking timestudy. Through the use of stop watches he measures and records time on a wide variety of operations in the foundry including core making, molding and cleaning operations. He pro-

poses improvements in standardization, efficiency, and simplification. This familiarizes him with timestudy and its application.

Scheduler—The apprentice assists in preparing a daily schedule, proposing an efficient sequence for the manufacture of gray iron castings in accordance with the building schedule of products.



Complete set of metal patternmakers tools which becomes property of the graduate upon completion of course.



Complete set of wood patternmakers tools which becomes property of the graduate upon completion of course.

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Metal patternmaking apprentice finishing metal match plate pattern prior to gating and risering.

Routing and Estimating—In this department estimations are made on costs of all foundry operations. These estimates are made available to the Accounting Department for standard costs. In addition, this department also routes jobs according to the type of equipment and material necessary for the job.

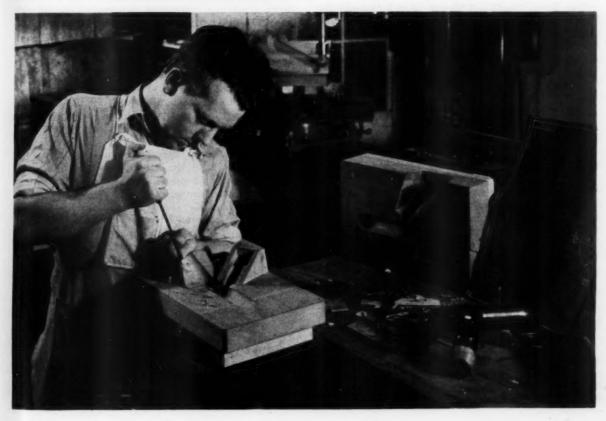
Assistant to Instructor (Molder Loose Pattern)—2 Months—The apprentice supervises and directs the immediate work on loose pattern molding unit which is manned by four apprentices. This is the same station on which he began his course but now he is responsible for the floor. Here the advanced apprentice plans the day's work, coordinates delivery of material and helps beginning apprentices on gating arrangements and molding technique.

Patternmaking-Wood and Metal

Rates of Pay—The term of apprenticeship is four years or 8000 hours. This time is divided into eight six-month or 1000-hour periods, rate increases being given at the completion of each period. These periods and rates of pay apply to both wood and metal patternmaking apprentices.

\$.88-Orientation Period

Wood patternmaker apprentice making mahogany core box which is considered advanced wood patternmaking.



.94—Second Period 1.00—Third Period 1.06—Fourth Period 1.12—Fifth Period 1.19—Sixth Period 1.26—Seventh Period 1.33—Eighth Period 1.44—Graduating Rate

Tools—Upon entering training, apprentices are issued a complete set of wood or metal patternmaking tools. The sum of five dollars is deducted from their checks each month until the tools are paid for. Upon graduation, apprentices are reimbursed for the full amount of the tools and they then become their permanent property.

Shop Training Schedule

Wood Patternmakers—The following is an outline of stations on which the wood patternmaking apprentices are given training during their four years. Stations are listed in the sequence that is followed when they are advanced from one job to another.

Elementary Wood Patternmaking—3 Months—On this station the apprentice assists a journeyman to make wood patterns for jigs and fixtures. He learns to operate all woodworking power machinery and hand tools used by wood patternmakers.

Elementary Metal Work (Bench)—3 Months—This station consists of mixed bench practice—first gating, fitting of driers and repair work. Here the trainee develops skill in the use of hand tools and proper procedures to be used for metal pattern work.

Core Room (Small)—1 Month—Bench Coremaking—At this station the apprentice learns how cores are made by actually doing it himself—but perhaps more important he becomes familiar with the different types of core boxes, driers, bedding frames and how they are used.

Small Core Assembler—Deals with learning the assembly of small cores—showing how pasting jigs, fixtures and rubbing gages are used.

Loose Pattern Molding—I Month—This enables the apprentice to become familiar with the importance of draft, core prints, parting lines and the various elements of molding which will aid in making better

Rough Casting Layout—3 Months—Sample castings are set up on a surface plate and checked thoroughly as to drawing dimensions. This training further develops his blueprint reading ability.

Trouble Shooting (Foundry)—3 Months—On this station the apprentice assists the regular pattern trouble shooter. He has an opportunity to learn some of the difficulties which are encountered in the core room and molding departments with core boxes and patterns. He makes minor repairs on the job or returns equipment to the Pattern Shop with detailed recommendations for repair. He is present when sample castings are made from new patterns to observe any corrections which may be desirable and needed.

Advanced Wood Shop—34 Months—During his term on this station the apprentice is assigned jobs in graduated stages of advanced wood patternmaking in line

with his knowledge and ability. He begins by making the more simple patterns and naturally receives a great deal of assistance from the apprentice supervisor, the foreman or a journeyman. He gradually progresses to the point where he is capable of working with a group of journeymen making parts of complex and intricate patterns.

Pattern Engineering (Estimate and Layout)—2 Months—Here the apprentice makes detailed drawings of pattern layouts from the engineering drawing showing cores, core prints, parting, draft requirement and material specification. He estimates costs of both wood and metal pattern equipment.

Shop Training Schedule

Metal Patternmakers—The following is an outline of stations on which metal patternmaking apprentices are given training during their four years. Stations are listed in the sequence that is followed as they advance from one job to another.

Elementary Metal Work (Bench)—3 Months—This station consists of mixed bench practice—first gating, fitting of driers, repair work. Here the trainee develops skill in the use of hand tools and proper procedures to be used for metal pattern work.

Elementary Wood Patternmaking—3 Months—On this station the apprentice assists a journeyman to make wood patterns for jigs and fixtures. He learns to operate all woodworking power machinery and hand tools used by wood patternmakers.

Core Room (Small)—1 Month—

Bench Coremaking—At this station the apprentice learns how cores are made by actually doing it himself, but perhaps more important he becomes familiar with the different types of core boxes, driers, bedding frames etc.. and how they are used.

Small Core Assembler—Deals with learning the assembly of small cores—showing how pasting jigs, fixtures and rubbing gages are used.

Loose Pattern Molding—1 Month—This enables the apprentice to become familiar with the importance of draft, core prints, parting lines and the various elements of molding which will aid in making better patterns.

Rough Casting Layout—3 Months—Sample castings are set up on a surface plate and checked thoroughly as to drawing dimensions. This training further develops his blueprint reading ability.

Trouble Shooting (Foundry)—3 Months—On this station the apprentice assists the regular pattern trouble shooter. He has an opportunity to learn some of the difficulties which are encountered in the core room and molding departments with core boxes and patterns. He makes minor repairs on the job or returns equipment to the Pattern Shop with detailed recommendations for repair. He is present when sample castings are made from new patterns to observe any corrections which may be desirable and needed.

Engine Lathe—3 Months—At this station the apprentice learns to operate an engine lathe by making parts for patterns or core boxes. He becomes familiar with the use of various tools, set ups and the operations of the machine.

Milling Machine—3 Months—At this station the

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apprentice learns to operate a milling machine by making parts for patterns or core boxes. He becomes familiar with the use of various tools, set ups and the operations of the machine.

Advanced Metal Patternmaking—22 Months—During this portion of the course the apprentice is assigned metal patternmaking jobs which gradually increase in complexity. He begins by making the simple parts in the repair of patterns or making and installing gates and risers. As he progresses in ability he makes the more intricate metal patterns entirely by himself or in conjunction with a journeyman.

Pattern Layout—4 Months—Here the apprentice learns to layout patterns on a surface plate—scribing lines for finish—and checking finished patterns for accuracy. Naturally he begins with simple patterns and progresses to the more complex ones.

Pattern Engineering (Estimate and Layout)—2 Months—Here the apprentice makes detailed drawings of pattern layouts from the engineering drawing showing cores, core prints, parting, draft requirements and material specifications. He estimates costs of both wood and metal pattern equipment.

Class Room Training

In addition to the various training stations throughout the shop all apprentices in the three training groups in the foundry, which we have been discussing, are given related classroom training. Classes are conducted in the form of group instruction in the class rooms by our instructors, with each class consisting of a maximum of 15 apprentices. Each apprentice receives a minimum of 600 hours of classroom work during his four years of apprenticeship, classes being conducted during regular working hours for two 1½-hour periods each week.

In presenting the subjects the theory and basic fundamentals are discussed with the aid of text books and practical applications, and demonstrations. Sound films and charts are used extensively throughout the course. Each student has sufficient text books to serve as a home library. Complete records are kept on the progress made by each apprentice.



Apprentice training group in classroom receiving instructions in engineering drawing.

Related Classroom Material

Since the foundry and pattern shop courses are so closely related all apprentices receive practically the same classroom training. The following subjects are given to all three groups.

Blueprint Reading and Shop Practice—40 Hours—The principles of orthographic projection are explained. There is practice in developing three views of an object. Freehand sketches of various shaped parts are made. Isometric drawing is presented to aid in visualizing the actual shape of an object when three views of it are shown. Engineering Drawing Standards of the Caterpillar Tractor Co. are studied and discussed to acquaint the apprentice with engineering drawing practices. After the apprentices are familiar with the fundamentals of drawing, actual production blueprints are studied.

History of Foundry and Foundry Materials—18 Hours—A brief history and development of the metal casting industry is given, and the various materials used in the foundry are discussed regarding source of supply, uses and composition.

Company Organization and Products—12 Hours—A better understanding of the products we manufacture and their use under all operating conditions is presented. The principles of the Diesel engine and track-type tractor are studied and many engineering features of our equipment are discussed. Also presented is company organization. During this class, the apprentice has an opportunity to visit the proving ground and operate a track-type tractor.

Mathematics—48 Hours—Every effort is made to develop a good understanding of industrial mathematics. There is a review of arithmetic, and first-year algebra. Logarithms and the slide rule are studied so each can be used in solving geometric or trigonometric problems. Practical problems are obtained in the shop and used whenever possible.

Coremaking—26 Hours—This subject includes more detailed study of materials and the description and applications of the machines and tools used in coremaking. Core sand mixtures and their application for certain classes of work are discussed. Various types of core binders, the action in the core ovens, baking temperatures, core washes, cleaning, venting, assembling of cores are studied in detail.

Materials and Processes—22 Hours—This subject is designed to give the apprentice a clear understanding of the materials used in our products. Each common material has certain physical and chemical properties. Basic methods of forming such as casting, forging, stamping, extruding and powder metallurgy are included. The nomenclature used in various manufacturing processes is discussed.

Molding—36 Hours—This subject includes much detailed information on molding materials, the description and application of various types of molding machines, construction of sand molds, mold pressure, gating, risering and casting defects.

Applied Trigonometry—60 Hours—Solution of technical problems in the shop often requires a good working knowledge of trigonometry. Basic fundamentals of the subject are taught from the textbook which is

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supplemented by practical problems arising in inspection, layout and various other operations. Whenever possible, the parts are brought into the classroom and together with the blueprints the necessary calculations are made.

Heat Treatment and Metallurgy—45 Hours—A discussion of the manufacture of steel serves as an introduction to this subject. Motion pictures aid in visualization of various processes. Such operations as annealing, direct hardening, carburizing, induction hardening and cyaniding are discussed and observed. Basic fundamentals of ferrous metallurgy are studied.

Industrial Physics—60 Hours—This subject includes study of forces and velocities, work, power and energy, friction, elasticity of materials, fluids, heat, principles of light and sound, magnetism and electricity. Emphasis is placed on practical application of fundamental physical principles. Demonstrations are the keynote of the course. They may take place in the class:coom, physical test laboratory or the shop as the subject may require.

Casting Defects—36 Hours—A general review of casting defects more detailed than those already discussed.

Engineering Drawing—69 Hours—Neatness of drawing depends much on the style of lines, lettering and spacing, so these fundamentals are stressed from the very beginning. As each new phase of engineering drawing is encountered the basic drafting principles are explained and then the apprentice is required to make types of drawings that involve these principles.

Pattern Design—84 Hours—This work consists of having the apprentice make a complete layout of pattern equipment from an engineering drawing. He marks prints for pattern and core box partings; then draws complete specifications for making equipment, paying particular attention to locating patterns for gating and dimensioning equipment to fit foundry machines.

Economics and Industrial Organization—30 Hours— In this class the apprentice is given a general course in essentials of economics as it applies to every day life. The course is based on textbooks but the ordinary problems of economics are simplified through use of familiar illustrations and examples.

Pattern Construction—14 Hours—In this classwork terminology used in the Pattern Shop is discussed. Meanings of various terms, such as cope, drag, core box, knitting and bedding frames, are good examples. Shop methods and procedures are shown, such as mixing glue, sharpening wood tools—both power and hand. Selection of wood and the proper method of laminating for pattern construction is illustrated.

In addition to these the following two classroom subjects are given to foundry apprentices only.

Foundry Sands—10 Hours—Characteristics of good core and molding sands are discussed in greater detail. Descriptions and method of operating various sand testing apparatus to determine sand characteristics are studied.

Cupola Practice and Cast Iron Metallurgy—60 Hours In this class, construction, operation and principles relating to cupola operation are discussed in detail. Materials used in the cupola are covered thoroughly. Considerable time is spent on calculating "Charges" for the cupola in order to arrive at various metal specifications. This class also covers elementary cast iron metallurgy starting with the mining of the ore, smelting process, various melting furnaces, chemical composition, solidification and cooling (iron-carbon diagram) types of cast iron, physical properties, classification of iron castings, alloy additions and heat treatment.

In addition to the principle group of subjects given to all groups the following related classroom subject is given to metal patternmaking apprentices only.

Gearing and Indexing—45 Hours—Contents of this subject include development of the gear from a simple spur type to the present universally used involute design and the ordinary variations. The student will actually develop the tooth outline of several sets of gears applying principles learned in this course.

Summary

We have found that selection of trainees is highly important in any successful training program. As brought out in the beginning the most important phase of selection is the personal interview. This is verified by observations during the initial six months of training.

Shop stations have been arranged so that the first half of the course consists essentially of developing skill while during the last half emphasis is placed on developing and broadening overall knowledge.

In the classroom we not only teach subjects necessary to the trade but also general subjects such as economics and industrial organization, which tend to broaden the appreciation of industry in general and the system of free enterprise.

Conclusion

For those young men who for one reason or another are unable to obtain a college education the apprentice training courses, such as these, offer an opportunity to increase their knowledge while learning a trade.

These graduates by continuing to apply themselves after graduation stand ready to take advantage of opportunities as they may arise later.

Of course they cannot all hope to become members of management because that requires qualities other than knowledge alone, but they have a skill higher than the average and can fill jobs commensurate with that skill.

In sponsoring young men to the completion of these courses we have expended considerable time, effort and money but we believe we are fully repaid by the fact that the level of the whole organization has been raised by absorption of these trained men into it.

During the past twenty or more years with rapid increase of the plant personnel, we would have been under a most serious handicap without a supply of trained personnel to furnish key men throughout the organization. As a matter of fact, it has never been necessary for us to employ anyone from without the organization in a key position. We believe it to be the responsibility of industry to provide adequate training programs to increase levels of skill demanded by today's production problems. Costs must be reduced.

CORROSION AND STRESS CORROSION PROPERTIES OF A HIGH STRENGTH ALUMINUM-ZINC-MAGNESIUM-COPPER CASTING ALLOY

By

R. A. Quadt* and E. C. Reichard**

THERE IS CONSIDERABLE INTEREST in aluminum casting alloys that produce high strengths without resorting to expensive heat treatments. These alloys enjoy a combination of mechanical properties equivalent or superior to conventional heat treated alloys such as Al-4 Cu (195), Al-7 Si-0.3 Mg (356), and Al-5 Si-0.5 Mg-1.3 Cu (355).

One such group of aluminum alloys utilizes zinc and magnesium to produce an intermetallic compound as the principal hardening agent. Precipitation of this compound from the solid solution produced during solidification of the alloy, results in marked increases in the yield and tensile strengths. This precipitation proceeds naturally at room temperature, most of the aging taking place within the first two or three weeks after casting (Fig. 1).

A traditional distrust of the corrosion resistance of aluminum alloys containing appreciable quantities of zinc has retarded their acceptance, but recently published^{1,2,3} test results have shown certain composition ranges to possess a corrosion resistance adequate for exacting engineering requirements. It is the purpose of this paper to describe the results of corrosion tests performed on a self-aging, high strength, sand cast aluminum alloy containing nominally: 7.5 Zn, 0.8 Cu, 0.4 Mg.

Immersion in Salt Water

Standard half-inch diameter cast-to-size tensile specimens were procured using the casting design shown in Fig. 2. The alloy, composition A of Table 1, was cast at 1250 F into green sand molds. Specimens were aged for two months at room temperature before starting the tests.

In general, the method of conducting total immersion tests complied with the requirements of ASTM specification B185-43T. All specimens were tested with the casting skin intact. Following solvent vapor degreasing the bars were hung in the corroding medium by means of glass hooks. The tests were par-

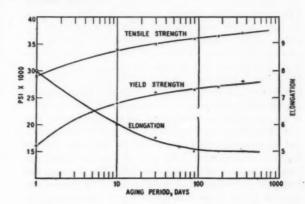


Fig. 1-Curves showing natural aging properties of 7.5 Zn, 0.8 Cu, 0.4 Mg alloy.

alleled with control bars which were subjected to the same temperature conditions (95 F \pm 1 F) as the corroding bars so that aging effects could be evaluated. The standard or control bars were sealed into glass tubes to protect them from the corroding environment. All tests were conducted in duplicate.

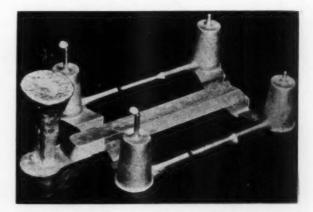


Fig. 2—Sand casting to produce two ½ in. diam castto-size tensile specimens.

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^{*} Formerly Research Metallurgist and Director of Aluminum Developments and ** Research Metallurgist, American Smelting and Refining Co., Barber, N. J.

| TABLE 1 | | | | | |
|-----------|------|------|------|------|------|
| | A | В | C | D | E |
| Copper | 0.83 | 0.20 | 4.10 | 0.95 | 0.92 |
| Magnesium | 0.55 | 0.00 | 0.00 | 0.48 | 0.43 |
| Zinc | 7.91 | 0.08 | 0.08 | 7.90 | 7.80 |
| Silicon | 0.18 | 4.98 | 4.98 | 0.19 | 0.18 |
| Manganese | 0.12 | 0.16 | 0.16 | 0.30 | 0.11 |
| Iron | 0.34 | 0.61 | 0.61 | 0.54 | 0.41 |
| Titanium | 0.09 | 0.01 | 0.01 | 0.11 | 0.12 |
| Chromium | 0.15 | _ | - | 0.17 | 0.14 |

At the end of each test period the corroded bars were cleaned of corrosion products by dipping in concentrated nitric acid at room temperature and scrubbing with a bristle brush under running water. Tenliter battery jars contained in a 95 F water bath were used for conducting the immersion test. An aerated 3 per cent sodium chloride solution was the corroding medium which was checked daily and adjusted for losses. The solution was aerated by blowing air through it at a rate in excess of 150 cc per minute per liter of solution. An aluminum thimble was used to break the air stream into small bubbles. These air bubbles were introduced at the base of a glass chimney placed over each aerator so as to prevent impingement of the stream of air bubbles on the test specimens. The rate of air flow was measured with calibrated differential manometers and controlled so that the desired flow was maintained. This rate of flow provided constant movement of the salt solution. The air used was first passed through glass wool in order to remove suspended solids; then through a 3 per cent sodium hydroxide solution to remove carbon dioxide and sulphur compounds; and finally through a water wash

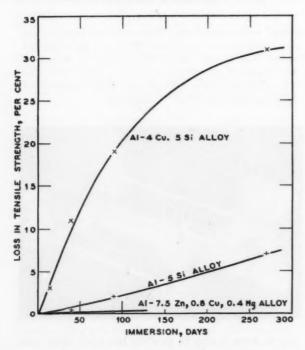


Fig. 3—Change in tensile properties resulting from various immersion periods in 3% aerated NaCl solutions.

bottle which served to humidify the air and avoid crystallization of the salts in the pores of the aerator.

Six specimens of the same alloy were generally tested per battery jar; hence a minimum of 75 cc of solution per square inch of specimen surface area was present. As bars were removed, this ratio of solution volume to specimen surface areas increased. The specimen positions were rotated daily to minimize local effects.

Immersion Tests on Other Alloys

For comparison purposes, similar immersion tests were performed on an Al-5 Si alloy (43), and an Al-5 Si-4 Cu alloy. These alloys are described as compositions B and C in Table 1.

Discussion of Immersion Corrosion Test Results-In Fig. 3 are plotted the results of the test conducted on the alloys under discussion. In this graph the change in properties as a result of corrosion is recorded as per cent. This technique permits comparison of the change in properties of these alloys despite their vastly dissimilar mechanical properties. The actual initial properties of these alloys are given in Table 2. Both the Al-7.5 Zn, 0.8 Cu, 0.4 Mg and the alloy containing 4 Cu aged somewhat during the testing period as a result of the exposure to the 95 F temperature. The aging effects could be evaluated since standard bars, aged under the same temperature conditions, were tested for each immersion period. Figure 3, therefore, shows the per cent change in properties between the corroded specimens and corresponding standard specimens that were aged for the same intervals of time at the same temperatures.

TABLE 2-INITIAL PROPERTIES OF ALLOYS PLOTTED

| IN FIG. 3 | | | |
|--------------------------------------|---------------------------------|------------------|----------------|
| | 7.5 Zn, 0.8 Cu, 0.4 Mg Alloy | Al-5% Si (43) | Al-5% Si-4%,Cu |
| Tensile Strength, psi | 38,900 | 22,200 | 24,100 |
| Yield Strength, psi (0.2% offset) | 29,600 | 8,300 | 13,100 |
| Elongation, % in 2 in | 1. 31/2 | 8 | 3 |

It is apparent that neither the Al-7.5 Zn, 0.8 Cu, 0.4 Mg nor the 5 Si alloy suffered any appreciable loss in properties. Weight losses were apparent, but corrosion occurred uniformly over the surfaces of both alloys and the lack of destructive pitting represents the primary reason for the negligible loss in properties.

The alloy containing 4 Cu, on the other hand, suffered appreciable losses in tensile strength and ductility. Weight losses were greater and severe pitting developed during exposure.

Under these standard corrosion conditions, therefore, the aluminum alloy containing 7.5 Zn, 0.8 Cu, 0.4 Mg has at least equivalent resistance to attack as compared to the 5 Si alloy, which enjoys a reputation for excellent corrosion resistance.

Stress Corrosion in Salt Water

Numerous data on the stress corrosion characteristics of the Al 7.5 Zn, 0.8 Cu, 0.4 Mg alloy is aerated 3 per cent sodium chloride solution were developed.

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eu, ns In view of the high yield strengths encountered and since it was desirable to use a direct loading constant stressing apparatus, subsized specimen diameters were more convenient. Despite the unfavorable aspects of smaller diameters in corrosion tests on tensile specimens, it was decided to standardize on ½-in. diam bars. These were produced in the casting shown in Fig. 4. The compositions tested were cast at 1250 F into green sand molds. All specimens were exposed with the casting skin intact. No grinding, filing, or polishing operations were performed on the bars.

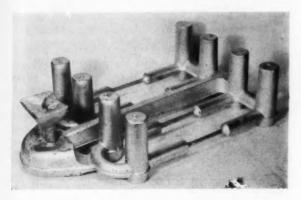


Fig. 4-Sand casting for producing four 1/4-in. diam cast to-size stress-corrosion tensile specimens.

Apparatus and Procedure

Figures 5a and b depict the apparatus used. It consisted of six stations for constant loading of the specimen by means of a system of levers and dead weights. The corroding medium was at room temperature and was circulated by means of an air lift which forced the solution to a reservoir where it was aerated prior to flowing over the specimens. The rate of aeration was in excess of 300 cc of air per minute per liter of solution. All air used was purified as previously described.

The gage lengths of the specimens were continually

and completely immersed in the corroding solution which flowed through the cylindrical glass section surrounding the specimens. The volume of solution was such that 600 cc of solution were present for each square inch of specimen surface.

Test No. 1

Composition A in Table 1 was cast into 1/4-in. diam tensile bars and aged five months at room temperature before being tested. The average properties of five bars during the stress corrosion test were:

| Tensile Strength, psi | 39,200 |
|-----------------------|--------|
| Yield Strength, psi | 31,000 |
| (0.2% offset) | |
| Elongation, per cent | 31/2 |

Specimens were loaded at various percentages of the yield strength starting at 100 per cent (31,000 psi), and subjected to the salt solution until fracture occurred. Broken specimens were replaced and the stresses decreased until no fractures occurred after 21 days.

The accelerating effect of stress acting with corrosion in causing losses in tensile properties is shown on Fig. 6. It is apparent that loads as high as 24,000 psi (78)

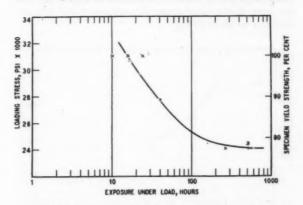


Fig. 6-Stress vs Breaking time. Room temperature aged alloy, Composition A, Table 1, exposed under load to 3% aerated NaCl solution.

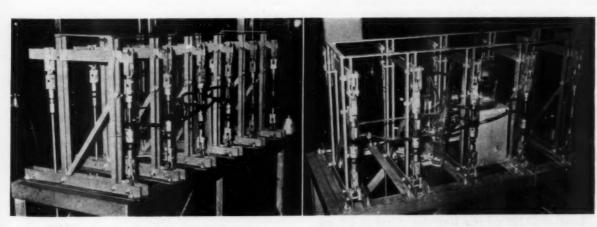


Fig. 5A (Left)-View of direct loading apparatus. Four stations assembled for stress-corrosion tests. Fig. 5B

(Right)-Front view of stress-corrosion apparatus. Note unstressed control specimens in rear.

per cent of the actual yield strength) did not have much effect. Specimens stressed at 90 per cent of the yield strength or more were sensitive to stress corrosion cracking. In this test, therefore, the critical stress is between 78 and 90 per cent of the specimen yield strength.

The three specimens still unbroken after being stressed at 24,000 psi in the corroding medium were removed and tensile tested. The average results were:

| Tensile Strength, psi | 37,000 |
|-----------------------|--------|
| Elongation, per cent | 3 |

Test No. 2

Composition D in Table 1 was similarly subjected to the combined effects of stress and corrosion. The specimens were room temperature aged for six months and the average properties of five bars were:

| Tensile Strength, psi | 39,500 |
|-----------------------|--------|
| Yield Strength, psi | 27,700 |
| Elongation, per cent | 5 |

The stress corrosion test results are plotted in Fig. 7. Specimens loaded at 24,400 psi (88 per cent of the yield strength) and higher failed within a measurable number of hours, while bars stressed at 23,500 psi (85 per cent of the yield strength) and lower did not fail through stress cracking.

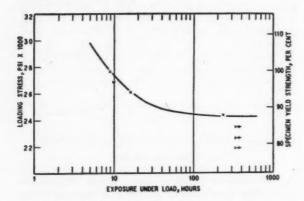


Fig. 7-Stress vs Breaking Time. Room temperature aged alloy, Composition D, Table 1, exposed under load to 3% aerated NaCl solution.

The stress corroded specimens unbroken after 14 days exposure were removed and tensile tested. There were no apparent differences in properties between these specimens and the average values were:

| Tensile Strength, psi | 37,100 |
|-----------------------|--------|
| Elongation, per cent | 4 |

The properties of test bars that were exposed to the same corroding medium for the same period, but not loaded were:

| Tensile Strength, psi | 37,600 |
|-----------------------|--------|
| Yield Strength, psi | 28,500 |
| Elongation, per cent | 4 |

Loads of 23,500 psi plus corrosion therefore did not cause appreciable changes in tensile properties of the specimens.

Test No. 3

Since it is often advantageous in commercial practice to achieve the final properties of the Al-7.5 Zn, 0.8 Cu, 0.4 Mg alloy rapidly, artificial aging at 250 F is employed. It was desired to learn the effect of this heat treatment on the stress corrosion properties. Specimens of composition E (Table 1) were produced and, after aging for 1 day at room temperature, they were artificially aged for 16 hr at 250 F. The average properties of four bars were:

| Tensile Strength, psi | 38,800 |
|-----------------------|--------|
| Yield Strength, psi | 27,300 |
| (0.2% offset) | |
| Elongation, per cent | 5.3 |

The results of the stress corrosion tests are plotted in Fig. 8. It is apparent that loads above 22,100 psi (81.4 per cent of the yield strength) produce stress cracking within a matter of hours, while the effect of stress was not detected below this value. This test is interesting since it indicates that the critical stress range is quite narrow. Below this rather precise threshold value stress cracking in this alloy does not appear to be a problem.

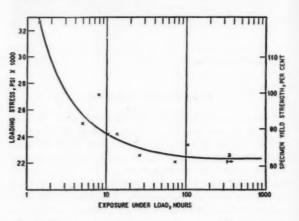


Fig. 8-Stress vs Breaking Time. Accelerated aged alloy, Composition E, Table 1, exposed under load to 3% aerated NaCl solution.

The unbroken castings were removed after 14 days and tensile tested. Again no differences were apparent between specimens stressed at 22,100, 19,900, and 16,700 psi. The average properties were:

| Tensile Strength, psi | 36,050 |
|-----------------------|--------|
| Yield Strength, psi | 28,200 |
| Elongation, per cent | 4 |

As in the previous test, it was desired to determine the difference, if any, in the properties of these test pieces upon being exposed to identical corrosive conditions in the absence of stress. Therefore, several 1/4-in. bars from this same heat were placed in a glass container and the solution circulated in similar fashion over the specimens during the 14-day test period. The average properties of these castings were:

| Tensile Strength, psi | 36,600 |
|-----------------------|--------|
| Yield Strength, psi | 27,100 |
| Elongation, per cent | 5 |

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It is apparent that stresses up to 22,100 psi do not cause significant reduction in the tensile properties of the specimens.

Effect of Specimen Diameter

As this work progressed it became apparent that the difficulties anticipated in using subsized test castings were quite real. Notch effects were magnified and foundry techniques required some refinement in order

to produce smooth surfaced castings.

Further, unstressed castings exposed to the salt solutions for as little as 14 days showed slight, but persistent, losses in tensile strength. This was contrary to the immersion test results obtained on standard 1/2-in. diam specimens in which no detectable losses occurred after 3 months' exposure. This unfortunate situation reflects the accentuated notch effects resulting from corrosion of 1/4-in. diam castings having one half the surface, but only one-fourth the cross-section of standard 1/2-in. diam specimens.

On the other hand, a recognized advantage of subsized specimens, in addition to the smaller apparatus needed, is the increased severity of the test. It seems reasonable that the threshold values revealed in the stress corrosion curves are probably lower than would be expected if 1/2-in. diam specimens were used. Since 1/4-in. sections probably represent a minimum thickness in most stressed commercial castings, factors other than stress corrosion would assume greater importance

in a particular application.

Summary

A high strength, self-aging, sand-cast aluminum alloy containing 7.5 Zn, 0.8 Cu and 0.4 Mg has been subjected to various corrosion and stress corrosion tests in 3 per cent aerated sodium chloride solution.

In immersion tests the alloy shows good resistance to attack; equivalent to the Al-5 Si composition that is commonly employed where good corrosion resistance

is required.

Specimens simultaneously corroded and stressed are unaffected by loads up to 80 per cent of the specimen yield strength. Beyond a threshold value located between 80 and 90 per cent of the specimen yield strength, stress corrosion cracking occurs. As a result of the high yield strength enjoyed by this alloy, some specimens stressed as high as 24,000 psi have not shown any loss in tensile properties caused by the combined action of stress and corrosion.

Acknowledgment

The authors are grateful for permission to report these data which represent part of a program of research at the Central Research Laboratory of the American Smelting and Refining Co. for its Federated Metals Division.

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DISCUSSION

Chairman: R. E. WARD, Eclipse-Pioneer Div., Bendix Aviation

Corp., Teterboro, N. J.

Co-Chairman: W. E. CARVER, Light Metals, Inc., Indianapolis.

J. J. WARGA (Written Discussion): 1 Messrs. Quadt and Reichard are to be complimented on the meticulous care with which they planned and performed their tests. Their work, as that of Sicha and Hunsicker, is an illustration of the amount of detailed data that must now be collected to establish an alloy for commercial use.

The investigation reiterates the point that stress-corrosion cracking does not occur except if some certain threshold limit of stress is exceeded during exposure. Below this threshold limit, failure if it occurs, is purely the result of corrosion and a long time interval is needed to cause failure; above the threshold limit, the combined effects of stress and corrosion cause rapid failure. The graphical presentation of this fact, which is shown in Fig. 6, 7 and 8 of the paper, is analogous to the S-N curve used to establish the endurance limit of metals and alloys. The

analogy may be closer than we realize.

To complicate the problem of stress corrosion, it appears that the threshold limit of stress can vary with the exposure medium. For instance, on a certain ternary aluminum-zinc-magnesium alloy we established that stress-corrosion cracking would not occur when it was exposed to 20 per cent salt spray for 500 hr at a stress level of 66 per cent of the yield strength. (The specimens were exposed as simple beams and the stress was measured in the outer fibers.) Yet this same alloy, in the same condition of heat treatment and surface finish, when exposed to a humidity cycle consisting of 6 hr at 150 F and 95 per cent relative humidity, followed by cooling to room temperature conditions in 18 hr failed within 2 days when the stress level was 66 per cent of the yield strength, and several specimens failed at a stress level of 40 per cent of the yield strength within 10 days.

For the particular alloy reported herein, our Materials Laboratory conducted corrosion and stress corrosion tests. As mentioned above, the specimen was a simple beam. No failure was encountered in salt spray exposures up to 500 hr at deflections corresponding to 70 per cent of the yield strength. No tests were

performed using the humidity cycle.

In "pure" corrosion, judged solely by appearance after exposure to 20 per cent salt spray we found that neither hare nor sulphuric acid-anodized samples could be considered satisfactory. The bare alloy, as a matter of fact, was but slightly better than alloy SG70A (Alcoa 356). On the other hand, the anodic film produced by chromic acid offered excellent resistance to corrosion by sodium chloride salt spray.

Mr. REICHARD: Mr. Warga's discussion is an interesting contribution to the knowledge on the performance of Al-Zn-Mg alloys. His observations are especially important in uncovering exposure conditions to be avoided in commercial use of these alloys. Relative humidity of 95 per cent, at 150 F is, however, an unusual condition of exposure. Unless the exposure condition is frequently encountered in commercial practice, the observations must be classed as having an academic interest rather than the basis for a valid test method for more extensive use.

It should be noted that there is hardly a metal known that does not have an alloy system susceptible to stress corrosion cracking by some specific environment. To be able to avoid such environments is important. However, test procedures for metals should be based as closely to their conditions of service. It was for this reason that the aerated sodium chloride medium was chosen for our work instead of sodium chloride solutions con-

taining hydrogen peroxide.

With regard to test methods, the complexity of reproducing and conducting corrosion tests in even commonly recognized environments is pointed out in the paper by Dr. Thomas P. May and A. L. Alexander on Salt Spray Testing, presented at the June, 1950, ASTM meeting. Some of the data of this paper have been given verbal discussion at corrosion meetings by Frank LaQue. In this paper the difficulties in conducting tests and evaluating results between just simple metals, no less alloys of any one system are pointed out. With the information contained in Dr. May and Alexander's paper in mind, the author hesitates to use unusual test environments for other than academic work. In fact, the validity of tests that are to duplicate service conditions must still be more clearly established.

¹ Metallurgical Engineer, Sperry Gyroscope Co., Great Neck, N. Y.

METAL COMPOSITION TESTS FOR THE STEEL MELTER

By

H. H. Fairfield, H. F. Graham and A. E. McMeekin*

ABSTRACT

A satisfactory estimate of the composition of carbon steels can be made in less than 2 min. The tests described make use of the hardenability properties of steel.

THE STEEL MELTER must make frequent tests to determine the condition of the metal in the furnace. Delays due to waiting for analytical reports from the chemical laboratory are costly. The cost of operating an electric furnace may be \$1.00 per minute or more.

There are a number of different procedures which may be used to determine metal composition. This report shows how hardenability properties of steel may be used for rapid test procedures. In 1938 Burns, Moore and Archer1 proved that carbon could be determined by correlation with a hardness test. The hardness carbon test was described by Kern,2 Locke3 and by Clark and Kowall.4 Vennerholm5 showed that a cast hardenability test could be used by the melter to estimate the composition of the metal in the furnace.

The ideas of the above mentioned investigators have been applied in the authors' plant to help steel melters determine bath analysis in less than 2 min.

PART I

Tensile Strength Estimated From Hardenability-If a bar of steel is heated to 1650 F and quenched by a water jet impinging on one end only, it will be hardened over a part of its length. The length of the hardened portion is an indication of the alloy content of the steel. This hardenability test for steel has many practical applications. Users of steel evaluate successive lots of steel by the hardenability test in order to ensure uniformity of heat treatment. Hardenability specifications have been set up by many purchasers of rolled steel. A summary of information on hardenability is contained in the Metals Handbook published by the American Society for Metals

Vennerholm⁵ reviewed the hardenability of steels and its importance to the steel foundry. This paper

relates how Vennerholm's suggestions were applied to metal control in a steel foundry making acid and basic electric steel.

 Chief Metallurgist, Assistant Metallurgist and Melting Supervisor, respectively, William Kennedy & Sons, Ltd., Owen Sound, Ontario, Canada.

This paper is restricted to a description of the methods used to estimate composition of medium-carbon steel which in the annealed condition must exceed the following mechanical requirements:

Tensile Strength, psi -70,000Yield Strength, psi -36,000Elongation, per cent -25Reduction of area, per cent-35

Experience with this grade of steel has shown that

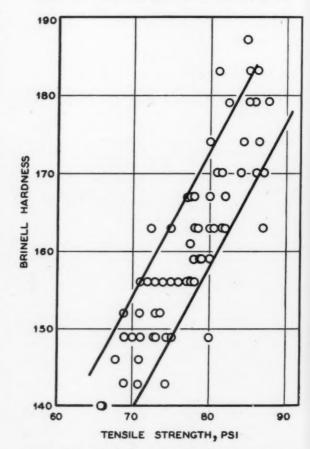


Fig. 1-Brinell hardness of annealed tensile test bars.

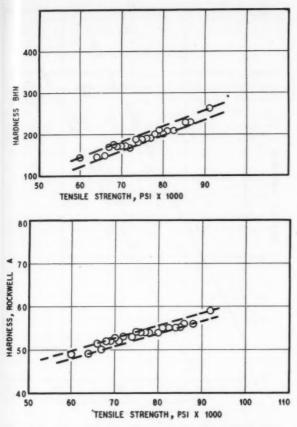


Fig. 3 (Above)—Rockwell A hardness of specimens described in Fig. 2.

Fig. 2-Brinell hardness of unquenched end of 1-in. long, 1-in.-diam hardenability specimens heated at 1650 F and quenched on one end.

if the carbon content is 0.20 to 0.25 per cent and the tensile strength does not exceed 85,000 psi, ductility requirements are usually met. The melters problem is to control the amount of hardeners so that the tensile strength will be in the proper range. This is facilitated by the hardenability test.

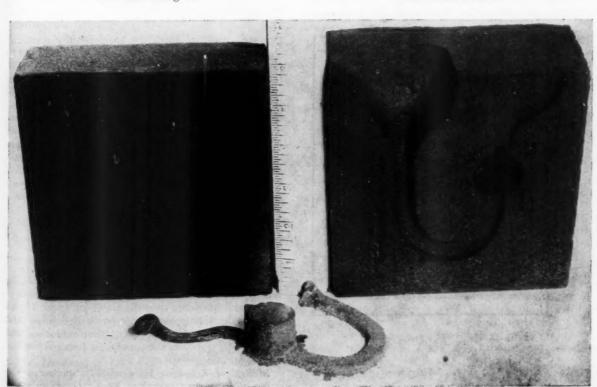
Hardness Related to Tensile

Brinell hardness readings were taken on a number of keel block castings which had been annealed. All tensile specimens referred to in this paper were annealed by heating to 1650 F and cooling at a rate of 100 to 200 F per hr. Figure 1 shows the relationship between Brinell hardness and tensile strength of annealed steel test bars cut from the keel blocks.

Machined Bar Hardenability Tests—From the ends of selected test bars, specimens 1 in. in diam and 1 in. in length were machined. These specimens were heated to 1650 F and quenched on one end only over a water jet. This is similar to the standard hardenability test except that the specimen is only 1 in. long. The hardness of the unquenched end was tested and is compared to tensile strength in Fig. 2 and 3. It is apparent that tensile strength is related to hardenability as indicated by a 1-in. diam, 1-in. long specimen.

Cast Hardenability Specimen-Figure 4 shows the

Fig. 4 (Below)—Cast hardenability specimen and core in which it is cast.



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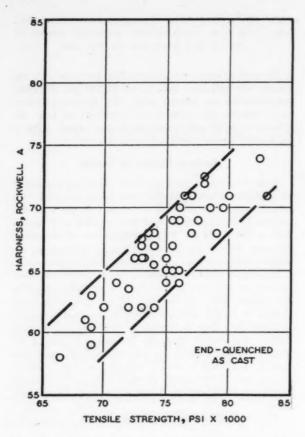


Fig. 5-Brinell hardness of unquenched end of cast hardenability specimens.

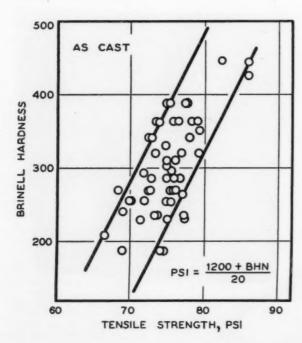


Fig. 6-Rockwell hardness of specimens described in Fig. 5.

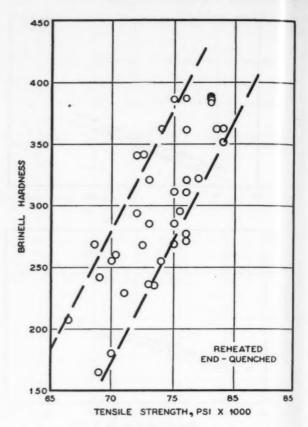


Fig. 7—Brinell hardness of unquenched end of cast hardenability specimens reheated and quenched on one end.

two sand cores in which the hardenability specimen was cast and a hardenability casting. The gates are so arranged that the specimen can be placed on a flat surface and is easy to position over a water jet. The procedure followed was to withdraw a test spoon of metal from the furnace and deoxidize the metal with aluminum, then cast the specimen. After 10 sec the core was broken and the specimen placed over a water jet allowing 60 sec for cooling. The specimen was quenched, touched to a grindstone and hardness readings were taken on the unquenched end. Figures 5 and 6 show the relationship between the as-cast hardenability test and tensile strength of annealed basic electric steel.

The relationship is described by the equations

Tensile, psi = Rockwell A + $8\frac{1}{2}$ Tensile, psi = $\frac{1200 + Bhn}{1}$

Reheated Cast Hardenability Tests—Some of the specimens described in Fig. 5 and 6 were heated to 1650 F and quenched over the water jet again. Figure 7 shows the relationship between hardenability and tensile strength. It is apparent that reheating and requenching improves the correlation between hardenability and tensile strength.

Method of Reheating Hardenability Specimens—By using a 3-tipped oxyacetylene torch, specimens could

be heated to 1650 F in 40 sec. Results from this fast heating were the same as when the specimens were heated in an oven for 1 hr.

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Basic Electric Steel

This steel is finished under a carbide slag. Before the final manganese additions are made, a carbon and hardenability test are poured. The target for hardness is 300 Bhn. If the steel is not up to this hardness, manganese additions are made using the assumption that 0.10 per cent manganese will give a 25 Bhn increase, and 0.02 per cent carbon will give a 50 Bhn increase.

Procedure With Acid Electric Steel—The bath is melted down and a vigorous boil produced. As the slag is deoxidized, carbon in the bath is adjusted to approximately 0.20 per cent. At this point carbon and hardenability tests are poured. The final manganese addition is based on the assumption that 0.10 per cent manganese will give a raise of 25 Bhn and 0.2 per cent carbon will give a raise of 50 Bhn.

Discussion—The cast hardenability test does not give the same correlation with hardness as the machined bar hardenability test. Leaving the runners and gates on the cast specimen may accelerate its cooling rate.

Although the reheated cast hardenability test is more accurate than the as-cast test, the extra time and trouble required may not be justified. At the authors' plant the as-cast test has been found sufficiently accurate for control purposes.

Gamma-ray examination has revealed that the test specimens described in this paper are not completely sound. They contain shrinks and porosity. A better correlation with tensile strength would be obtained if sound specimens could be cast. This problem has not been solved.

Before this test was applied, high tensile strengths were obtained when alloys were encountered in the scrap. Since the hardenability test detects the effect of any residual alloy hardeners, the melter compensates by adding less manganese or carbon.

Alloy steels can be controlled by means of the hardenability test. The size of the test specimen has to be increased for high alloy steels.

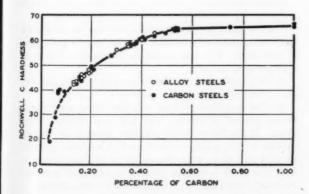


Fig. 8—Hardness of fully-quenched carbon and alloy steels.

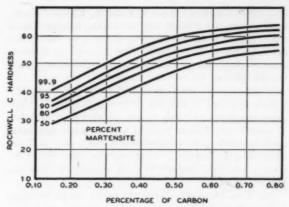


Fig. 9-Effect of per cent martensite and carbon content on hardness.

Conclusions

- 1. The tensile strength of a heat of carbon steel can be predicted during the melting operation by means of a cast hardenability test.
- 2. The cast hardenability test can be completed within 2 min. It can be done by the melter.
- 3. Predictions of tensile strength can be made only if the heat treatment of the tensile test bars is consistent from heat to heat.
- 4. The actual correlations shown in this paper hold true for the conditions under which they were made. A change in the test procedure or design of specimen would alter the hardenability correlation to tensile strength.
- 5. The quality of metal produced with the aid of hardenability control procedures has been equal or superior to the heats made previously with chemical control procedures.
- 6. The advantages of the hardenability control method are:
 - a. The melter can compensate for residual alloys.
 - No time is lost waiting for laboratory test reports.
- The hardenability test has been used to control acid and basic electric steel for several months. Carbon and alloy steels are made by hardenability control methods.

PART 2

Hardness Carbon Test

In 1938 Burns, Moore and Archer¹ demonstrated that the maximum hardness obtainable in carbon and alloy steels is dependent upon carbon only. Alloys do not affect the maximum hardness obtained on quenching as long as the structure does not contain austenite. The hardness of the quenched end of hardenability specimens has also been related to carbon content (see Metals Handbook, ASM, 1949 edition, p. 500).

Several investigators have applied the above principle to determine carbon content of liquid steel. Kern,² Locke³ and Clark and Kowall⁴ describe the hardness carbon test.

The essential requirements of the hardness carbon

test can be deduced from Fig. 8 and 9. Figure 8 shows how carbon determines the maximum hardness in a steel. Figure 9 shows the effect of per cent martensite upon hardness. It is evident that in order to develop a satisfactory test procedure drastic quenching must be carried out.

Attempts were made to obtain a 100 per cent martensite structure in samples of low-carbon steel. After many unsuccessful trials, it was decided that in order to obtain the desired cooling rate, a small specimen must be used. It was found that a pin 2 mm in diam could be fully hardened by quenching from 1800 F in water. The first method to work successfully was the pin test method.

Pin Test Method*—The procedure in performing this test is as follows:

1. Using a rubber bulb and a length of 2-mm ID glass tubing a specimen is drawn from the furnace test spoon.

2. The pin of metal is heated to 1800 F in a tube furnace and quenched in water, or

2A. The pin is heated to a white heat by connecting it into an electric circuit delivering approximately 60 amp at 10 v. This is followed by quenching in water.

3. Flats are ground on the pin.

* Suggested by Charles Locke, Armour Research Foundation, Chicago.

TABLE 1—HARDNESS OF FULLY HARDENED CARBON

| Rockwell A Carbon Content of Steel, % | | |
|---------------------------------------|-----------------------|---------------|
| Hardness | 2 mm diam Specimen | Flat Specimen |
| 58 | 0.08 | 0.09 |
| 60 | 0.09 | 0.105 |
| 62 | 0.11 | 0.12 |
| 64 | 0.13 | 0.13 |
| 66 | 0.15 | 0.15 |
| 68 | 0.18 | 0.17 |
| 70 | 0.21 | 0.19 |
| 72 | 0.24 | 0.22 |
| 74 | 0.27 | 0.25 |
| 76 | 0.31 | 0.28 |
| 78 | 0.36 | 0.32 |
| 80 | 0.40 | 0.37 |
| 82 | _ | 0.44 |

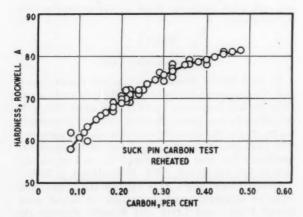


Fig. 10-Effect of carbon on hardness of quenched 2-mm-diam pin.

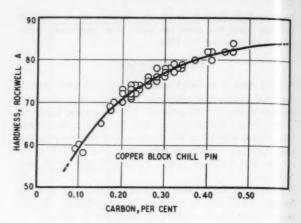


Fig. 11-Effect of carbon on hardness of 0.0625-in. fin cast in water-cooled copper mold.

4. Rockwell A hardness is determined.

5. Table 1 and Fig. 10 show the correlation of carbon vs hardness.

Treatment of Metal in Spoon—Experience with the test over several months showed that when carbon was below 0.18 per cent and manganese was also low, erratic results were obtained. It was reasoned that the metal was not fully hardened. A pellet of low-carbon ferromanganese was added to the metal in the test spoon to increase the hardenability of the metal being tested. This practice corrected the variation in low-carbon, low-manganese tests. It is recommended that if manganese below 0.30 per cent, hardener be added to the test spoon. An addition of 0.50 per cent is sufficient. It is also necessary to deoxidize the metal in the test spoon with aluminum wire. The amount of aluminum used does not influence the test.

Chill Mold Method—The reheating and quenching procedure was eliminated by casting directly into a water-cooled copper mold. A specimen 0.0625 in. in thickness is cast, and after cooling for 20 sec, is ready for Rockwell testing. The total time for this test is 40 sec. Figure 11 shows data obtained with the coppermold method. Figure 12 shows the copper mold at the furnace.

Conclusion

These conclusions are not applicable to steels which are partially or fully austenitic after quenching.

1. It is possible to determine carbon by means of Rockwell A hardness tests on fully-hardened specimens of cast steel. The test works in the range of 0.10 to 0.45 per cent carbon.

2. Full hardening may be assured by adding hardeners to the test spoon before pouring.

3. The hardness carbon test has been used for a 6-mon period to control acid and basic electric steel heats. It has been found accurate enough to control carbon within a range of 0.20 to 0.25 per cent in heats of acid and basic steel. Accuracy of the hardness carbon test itself is within plus or minus 0.02 per cent carbon in the range of 0.10 to 0.35 per cent carbon.

4. The pin test is probably more accurate than the copper-mold test. However the pin test requires greater care in execution, also it requires two men to



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Fig. 12-Water-cooled copper mold used to cast carbon test specimens.

make the pin test. The test specimen cast in the copper mold can be handled by one man, and it requires less time to complete.

5. Difficulties experienced by other users of the hardness carbon test may be due to the fact that they used specimens exceeding 1/8 in. in diam. Only thin sections of lower range carbon steel can be fully hardened.

Acknowledgment

Successful application of the tests described in this paper were made possible by the work and cooperation of Yuan Luan Sun, Robert Feltrin, John Strimas, John MacConachie, Robert Eagles and Morris Johns. The continued support and encouragement received

TABLE 2-HARDNESS CONVERSION

| Rockwell A | Brinell |
|------------|---------|
| 46 | 152 |
| 48 | 159 |
| 50 | 167 |
| 52 | 179 |
| 54 | 187 |
| 56 | 207 |
| 58 | 223 |
| 60 | 248 |
| 62 | 285 |
| 64 | 321 |
| 66 | 352 |
| 68 | 380 |
| 70 | 415 |
| 72 | 461 |
| 74 | 514 |
| 76 | 555 |
| 78 | 627 |
| 80 | 712 |

This table is derived from comparative tests made on specimens used for the investigation.

from Neil Kennedy, Supt. of Foundries, is also gratefully appreciated.

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DISCUSSION

Chairman: L. H. HAHN, Sivyer Steel Castings Co., Chicago. Co-Chairman: J. K. McBroom, Stainless Foundry & Engineering Co., Milwaukee.

H. H. Blosjo: 1 How do you account for the difference in hardness shown in Fig. 8 and 9? In Fig. 8, for example, the Rockwell C hardness of a fully quenched 0.20 per cent carbon steel is 49. In Fig. 9, the Rockwell C of 0.20 per cent carbon steel quenched to 99.9 per cent martensite is only 44. It would seem that these two hardnesses should be the same.

Mr. FAIRFIELD: Due to an error in typing the source of Fig. 8 and 9 was not indicated. Figure 8 is the work of Burns, Moore and Archer as indicated at the end of our paper. Figure 9 is given on p. 497 of the 1948 Metals Handbook. The different results

obtained are due to experimental technique.

MR. BLOSJO: We use this test in our foundry not for all heats, but where we need the very best carbon control. We, however, use the direct quench of the sample as it cools down from pouring. The sample is poured in a split steel mold producing a sample % in. square and about 3 in. deep, with a conical head on one end for pouring. The bar is stripped out of the mold in from 3 to 5 sec after pouring at which time the corners are a little cold, but the center is still hot. The bar is held in still air for about 15 sec during which time the temperature of the whole bar fairly well equalizes at a temperature estimated to be 1600 to 1700 F. The specimen is then quenched in 60 F water. The Rockwell C hardness is then taken in the center of one side and in a location of 1/2 to 1 in. from the small end.

How do you take the hardness, i.e., how many readings do you take and which readings do you accept as being satisfactory? Mr. FAIRFIELD: We take three readings and observe how close they average. If they are not close we keep up taking tests until we have three close values. Sometimes we may take five or seven

readings, one after another.

MR. BLOSJO: We take a minimum of three using the "C" scale. I frankly never used the "A" scale. In the "C" scale almost invariably the first reading will be low and will be thrown out. I do not know why that is. I think it is in the setting of the equipment. It does not take much movement in the equipment to give you a low reading. The next three readings are usually good. Again, if we have one that is out, we do the same as you, we throw it out until we get three readings that check closely.

G. E. Dalbey: 8 Have you had any difficulty in getting a sound sample in picking the metal up with a glass tube? Did you have to use pyrex tubes to make it stand up? Also, it is possible to quench fast enough in the glass tube to get consistent readings?

Mr. FAIRFIELD: We used ordinary lime glass tubing because it breaks away more readily from the specimen. Pyrex glass does not crack and then we have a hard time breaking it to get the specimen out. So we use ordinary glass tubing. We do have trouble getting a sound specimen especially when the metal is in the wild state though we deoxidize with aluminum. Those were two of the things that were working against us on that test.

¹ Metallurgist, Minneapolis Electric Steel Castings Co., Minneapolis,

² Metallurgist, Mare Island Naval Shipyard, Vallejo, Calif.

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We never knew when the pin was not sound. Once the metal is deoxidized the pin test is usually sound.

Mr. Dalbey: Can you quench fast enough to get constant hardness readings? Referring to the soundness of the pin test sample, we are having considerable difficulty in getting a solid cylinder. We have generally gotten a tube and it will spring when loaded on the Rockwell hardness tester.

MR. FARRIELD: We went down to 2 mm in diameter. I believe the smaller diameter pin is sounder and also subject to that effect. As to quenching it direct from the glass tube for hardness test, that can be done on high alloy steels which are almost air hardening. The point we are interested in is at the end of the boil where it is low on carbon. You cannot quench that fast enough from the glass tube. We have a carbon tube furnace, running at 1800 F. We pick up that pin and slide it into the furnace and watch it until it is hot. That takes about 50 sec. Then we have a pan of water right at the end of the tube. We just pull it out and quench it in the water. I believe that is necessary for accurate work on the pin test.

Mr. Blosjo: I want to qualify a statement made previously. A 3% in. square bar of plain carbon steel would not harden sufficiently to give you any results at all. We deliberately add a quantity of alloy to about 1½ lb of sample in the spoon to give us 0.75 Cr and 0.50 Mo before casting. When we do that we

check the curve in Fig. 8 very closely. In the lower carbon we are probably on an average of 1 point Rockwell "C" below the curve in Fig. 8.

A. L. TARR: The Dow Chemical Co., Midland, Mich., has a sampling gun (or suction pistol) with which they draw up, in a glass tube, a fine rod of metal which they use as a sample in their spectrographic test. The gun operates by suction. The sample it makes could be used in this technique very well. The gun looks like a large pistol and costs about \$50.00. To get a sample, you put a tube of glass in one end and pull the trigger. In about a fraction of a second you have the sample. It hink a one-man direct quench technique might be carried out on the sample provided by this procedure with the gun. It is a tricky looking thing which ought to appeal to the sample man or melter because in an instant sampling is completed. You do have to hold the gun at a perpendicular angle. They use a tube of a little less than a ¼-in. diam but the gun can be adapted to a small diameter tube.

Then there is an evacuated tube that the Boder Scientific Co., Pittsburgh, puts out. They cost \$.15 each in lots of 100. Have you tried any of these tubes?

MR. FAIRFIELD: No we have not. We used rubber bulbs.

³ Metallurgist, Engineering Corps., Ft. Belvoir, Va.

STEEL QUALITY AS RELATED TO TEST BAR FRACTURES

By

H. H. Johnson and G. A. Fisher*

Tensile test is recognized as a standard of acceptability in the testing of steel. It is the most frequently employed mechanical test used by the steel

castings industry.

It is generally realized that, by the mechanical testing of materials, such as samples from a heat of steel, it is possible to obtain numerical values indicative of the properties of the steel under test. In addition to these measured properties, the appearance of the fracture itself is usually noted and expressed (and usually recorded) in rather qualitative terms. If there are obvious imperfections or flaws in the fracture, or if the fractures occurs outside the middle third of the gage length, the results are usually disregarded as being unrepresentative of the material itself that is under test. If the fracture is seemingly sound in appearance, its nature is noted as to whether it is irregular, angular, partially cupped or a full cup and cone fracture.

It is generally agreed that the type of fracture is related to the quality of the steel and, for example, that an unflawed bar breaking with an irregular or an angular fracture probably represents steel of poorer quality than a bar that breaks with a cupped fracture.

This observation of the differences in appearance of fracture exhibited by test bars representing presumably similar heats of steel, is not peculiar to cast test bars alone but has been noted also in bars representing wrought steel. For example, Wells and Mehl1 have pointed out that "Results-show that, on the average, transverse reduction of area values for test pieces having an angular break are lower than those for pieces having a cup and cone break, and there is some justification for believing that if the cause responsible for a particular angular fracture had been removed so that the broken test piece considered would have had a cup and cone fracture, then the transverse reduction of area value for that piece would have been higher." They conclude that for wrought steel "-that (a) an angular fracture in itself is no criterion for low ductility and (b) there is little justification for suspecting

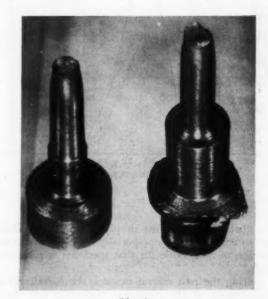


Fig. 1

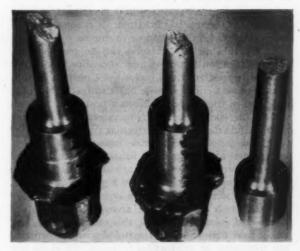
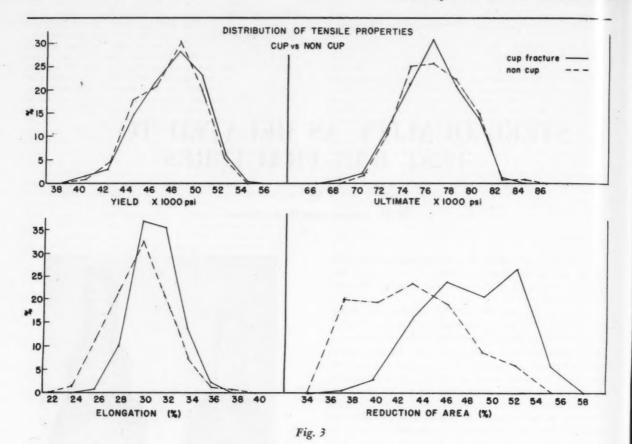


Fig. 2

^{*} Chief Metallurgist and Quality Control Engineer respectively, Sharon Works, National Malleable and Steel Castings Co., Sharon, Pennsylvania.



the quality of a product simply because a specimen has an angular fracture, especially if the transverse reduction of area value for that specimen is relatively high."

During the past several months, the Metallurgical Department of the National Malleable and Steel Castings Co. has given considerable thought to a consideration of some of the factors that might affect the quality of the steel which would be reflected in the character of the test bar fractures. It is some of these considerations that we propose to discuss.

In general, test bar fractures can be grouped into two classifications: cupped and noncupped fractures. Typical cupped fractures include not only those that form a perfect cup and cone but also fractures where each half of the test bar exhibits a portion of the cup and of the cone (Fig. 1). Noncupped fractures will then include the ragged or irregular types, the angular type, and (occasionally) a flat fracture (Fig. 2).

In order to measure the relationship that might exist between the type of fracture and the level of the physical properties exhibited by a group of test bars, test results were selected which represented some 400 heats of Grade B steel, whose analyses fell within the established control limits for this grade of steel, and whose heat treatment consisted of the usual normalizing treatment in production furnaces. Of these test bars 200 had broken with cupped fractures and the other 200 bars had broken with noncupped fractures. The physical test results are exhibited in the form of

frequency distribution charts for each of the four properties measured (Fig. 3). It is obvious that the yield and ultimate strength distributions are similar for both types of fracture, but that the level of ductility, as measured by both elongation and reduction of area is markedly less for the group of noncupped tests and that the spread of results is considerably greater for the noncupped tests than for the cupped tests.

Ultimate Strength × 1000 psi

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by

To present the picture in another way, it has been shown by Schwartz² that there is an approximately linear relationship existing between the tensile strength and the elongation for normalized and tempered steel. An approximately straight line relationship correlates the tensile strength and the reduction of area for normalized and tempered steel within the range of tensile strength with which the present investigation is concerned. The reference work points out, however, that the scatter of results for test data taken from bars that broke with a cupped fracture is considerably less than for data from bars that broke with noncupped fractures. This is especially true for the reduction of area data.

The Elongation-Tensile Strength relationships for the data of the present investigation are presented in Fig. 4, showing the relationship for both cupped and noncupped groupings. It is obvious, by inspection, that the relationship is better for the cupped fractures than for the noncupped fractures.

Similarly, the Reduction of Area-Tensile Strength



Cupped Fractures
Coefficient of Correlation = 0.46

Non-Cupped Fractures
Coefficient of Correlation = 0.17

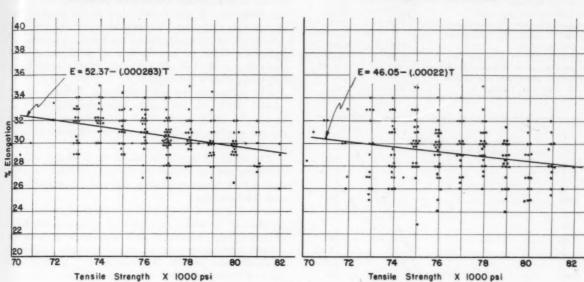


Fig. 4

ELONGATION VS TENSILE STRENGTH

Cupped Fractures
Coefficient of Correlation = 0.35

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Non-Cupped Fractures Coefficient of Correlation = 0.24

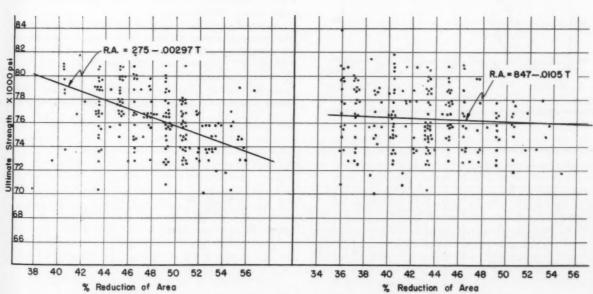


Fig. 5

relationships are exhibited in Fig. 5. Here the scatter of results for the noncupped fractures is so great that practically no relationship exists while for the cupped fracture data the correlation is real.

The significant statistical characteristics for the two sets of data are presented in Table 1.

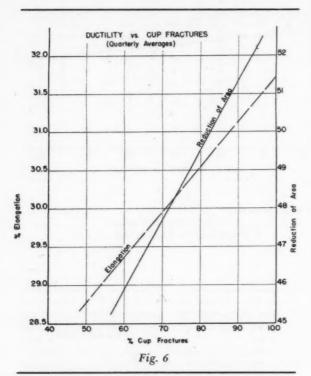
Remembering that perfect correlation is indicated by values of + 1.0 or - 1.0 and that entire lack of

correlation is indicated by zero, it would seem that, while none of the correlations are very good, those for the cupped fractures data are considerably better than those for noncupped fractures. Especially is this true for the reduction of area relationships.

The question can then be raised "How reliable is the coefficient of correlation?" A major factor in determining that reliability is the size of the sample,

TABLE 1

| | Cupped | Noncupped | |
|---|--------|-----------|--|
| Number of Samples | 200 | 200 | |
| Mean Elongation, per cent | 30.72 | 29.23 | |
| Standard Deviation of Elongation, per cen | t 1.85 | 2.96 | |
| Correlation Coefficient (Elongation | | | |
| and Tensile) | -0.35 | -0.24 | |
| Mean Reduction of Area, per cent | 47.94 | 42.67 | |
| Standard Deviation of Reduction of Area | 4.01 | 4.54 | |
| Correlation Coefficient (Reduction | | | |
| of Area and Tensile) | -0.46 | -0.17 | |



since the smaller the number of items the greater the possibility of accidental co-variation. Tables and charts are available showing for different sample sizes, drawn from normally distributed universes, the highest coefficient of correlation that might appear by chance once in 20 times (called the 5 per cent level).

In other words a correlation coefficient is said to be significant when the probability of its chance occurrence is less than 5 per cent and highly significant when less than 1 per cent.

The coefficients we have obtained (-0.35, -0.24, -0.46, -0.17) are all highly significant by this test except the -0.17 value which is significant. This observation pretty much confirms the relationships discussed above.

The standard deviation values measure the scatter of results and are smaller for the cupped fracture data than for the noncupped data, especially in the elongation relationships.

A similar study has been reported by Igelman and Nussbaum³ in which they plotted quarterly averages of percentage elongation and reduction of area as related to the percentage of cupped fractures produced in those same periods. Here again is evidenced the increase in average level of both the ductility measurements with the increase in the occurrence of cupped fractures.

From the data thus presented some of the characteristics of cupped and of noncupped fractures have been evaluated to point out that the differences represented by the type of fractures may be quite significant in measuring the quality level of steel production of the type under consideration.

As noted previously, interest has centered mainly in the test results from medium carbon steel, normalized or normalized and tempered, to meet A.A.R. Grade "B" or "C" specifications. The present data is taken from heats produced by the acid electric process, which have been made following conventional practice, but using titanium-boron and calcium silicon for deoxida-

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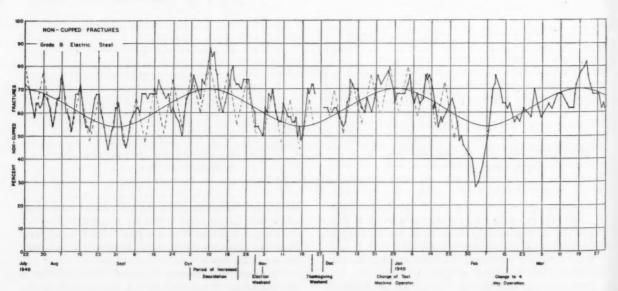


Fig. 7

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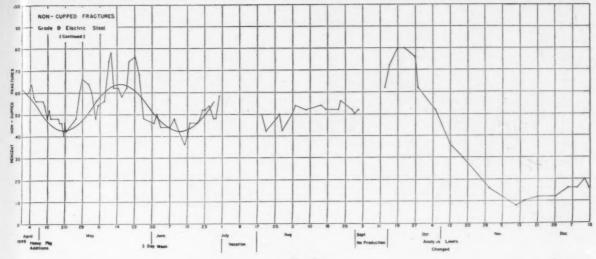
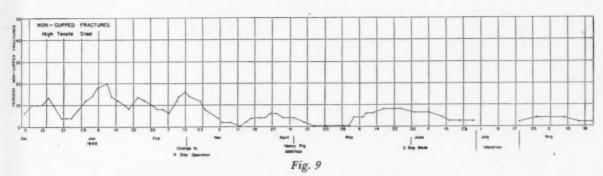


Fig. 8



tion instead of the more conventional aluminum and calcium silicon addition. In general the tensile test specimens are machined from tensile test bars with obvious flaws are not considered.

In order to obtain a picture of the process performance, the "moving average" technique of charting was selected. For the construction of the chart, the percentage of noncupped fractures exhibited by a group of test bars from 50 consecutive heats was used as the value for the first point. For each successive point, the values for the first ten heats involved in the first group are dropped, and ten additional values are added, from which the percentage of noncupped fractures is recomputed for the second group of 50 values. Thus the second point would include the fractures for the 11th to the 60th heats, the third group will include results from the 21st to 70th heats, etc. These results are plotted on a time basis of calendar days.

Such a chart has been maintained for Grade "B" steel results for a period of some 18 months and is exhibited in two parts (for ease of reproduction) in Fig. 7 and 8.

Such charting tends to smooth out minor fluctuations in the process results but shows the long range cyclic trends, if the grouping is so chosen that it represents the cycle that the process is trying to follow. For example, using the 50-point grouping and plotting the results against calendar days it would seem that a rather definite 80-day cycle exists over a period of perhaps 240 days (as shown in Fig. 7 and Fig. 8). It then becomes nonexistent probably because the rate of shop operation decreased considerably, changing from a 5-day to a 4-day and finally to a 3-day per week rate of operation, since all results were plotted on a calendar day basis.

Likewise a marked 8-day cycle is evident for a period of perhaps 90 days and then becomes erratic.

Although both of the cycles are fairly definite over a sufficiently long period of time to be significant, no explanation is advanced to the cause of such regular fluctuations. It is entirely possible that if the data for the period since the cycles became erratic were regrouped and the moving averages then determined, a grouping could be found that would result in a cycle of some significant magnitude. This is only a hypothesis and did not seem worth additional study.

Notations have been made on the charts of changes that have been made in practice from time to time which may have been of significance in affecting the type of fracture produced. For example, three changes which seem to have had a measurable effect were as follows:

- 1. An increase in the quantity of deoxidizers used during a 20-day period, which was marked by an increase in the percentage of noncupped fractures produced;
- 2. An increase in the level of melt-in carbon, necessitating greater oxidation of the heat which resulted

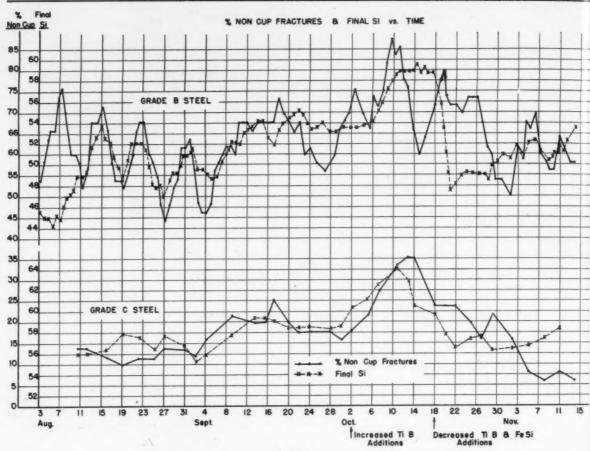


Fig. 10

in a change in frequency of the cycle from an 80-day to a 50-day cycle and also in a decrease in the percentage of noncupped fractures produced.

3. A change in the control level for both carbon and manganese which was marked by a considerable decrease in the frequency of occurrence of noncupped fractures.

Because other fluctuations exist in the level of the rate of occurrence of noncupped fractures, it is probable that other significant changes must have been made in the process from time to time, some of which can be evaluated.

As noted previously, the discussion has been confined thus far to the results from test bars made to meet Grade "B" specifications, and in general the heat treatment has consisted in normalizing treatment only. If we consider another series of tests from heats made to meet Grade "C" specifications, the moving average technique gives a chart such as is shown in Fig. 9. The melting process was the same for this type of steel as it was for the Grade "B" steel, but it was alloyed and heat treated to meet the Grade "C" requirements. For ease of comparison, typical analysis, heat treatments, and physical requirements for the two grades of steel are presented in Table 2.

A casual comparison of Fig. 7 and 8 for Grade "B" steel and Fig. 9 for Grade "C" steel indicates the con-

siderably greater amount of "cupping" produced in the Grade "C" steel as compared with that found in Grade "B" steel. Further study was indicated to determine what factors might be of significance in causing such differences in the type of fracture produced by the two grades of steel.

It would seem obvious that those factors which act to decrease the ductility of the steel should also produce an increase in the amount of noncupped fractures. In general, any conditions that cause poor inclusion form in the steel, or tiny discontinuities due to gases, or similar interruptions in the matrix would act to produce nonuniform conditions of rupture in testing and hence noncupped fractures.

It was further evident early in the investigation that a factor that was seemingly predominately effec-

TABLE 2

| Grade Analyses Heat Treatmen | | Minimum Tensile Requirements | | | | | | | |
|------------------------------|------|------------------------------|------|-------|----------|--------|------------------------------|----------------------|----------------------|
| | | | | | | | Reduc- Elonga- tion of | | |
| | С | Mn | Ni | | | | Ultimate Strength, psi | tion, per cent | Area, per cent |
| - | | | | | rmalized | 38,000 | 70,000 | 24 | 36 |
| "C" | 0.28 | 1.25 | 1.20 |) " 8 | k Drawn | 60,000 | 90,000 | 22 | 45 |

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tive over one period of time would be inoperative over another period of time or at least be of secondary importance to some other factor. For example, in Fig. 10 are reproduced the moving average charts for the per cent of noncupped fractures in both classes of steel for the first three months plotting time (taken from Fig. 7 and 9). The plotting for the silicon content is superimposed on the chart, using the same moving average technique, so that each point represents the average value for the same 50 consecutive heats. A period of increased deoxidation (which has been mentioned as one of the three significant changes) is included and also a subsequent period of decreased deoxidation and of decreased silicon addition. The correlation between the frequency of occurrence of noncupped fractures and silicon content seems to be quite good for most of the interval under discussion but loses the cycle after the silicon addition is reduced.

The data are replotted in Fig. 11 to show the rela-

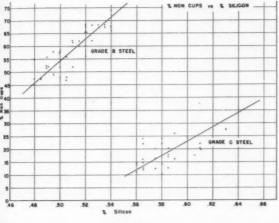


Fig. 11

tionship between silicon and the percentage of noncupped fractures for both types of steel to show the quite excellent correlation which exists for the period of time under consideration. However, when other time intervals are selected, a wide scatter of data results with practically zero correlation between the silicon content and the type of fracture produced. The only explanation offered is that the silicon content is not particularly detrimental if it is added to the metal in the form of an alloy but if it is reduced by high temperature from the slag or lining it frequently acts to cause a poor inclusion form to exist in the steel.

Unfortunately we do not have enough data from this period to provide formal proof for this reasoning. However, the silicon values for most of this period were well over the 0.45 to 0.50 range (which was the level we were presumably aiming for). This in turn would indicate that a silicon reduction was taking place during the refining period, which was probably causing an unfavorable inclusion form.

The second change mentioned above is that of an increase in melt-in carbon and increased oxidation during the refining period. This change was brought about by an increase in the pig iron addition in the

charge so that the level of melt-in carbon was about 0.40 carbon. There were practically no heats melting in with less than 0.35 carbon. Consequently an increased amount of ore was used which resulted in an increase in the action and duration of the boil.

The frequency of occurrence of noncupped fractures had been following an overall cycle of about 80 days (as shown in Fig. 7) and this now changed to one of about 50 days frequency (as shown at the beginning of the chart, Fig. 8). May we add that the plotting of these charts was all done on a single continuous chart in the laboratory which was then broken into two charts because of convenience in reproduction. Had we been able to reproduce the entire chart, the change in frequency would have been more clearly marked.

The level of the per cent of noncupped fractures also changes at this time from about 62 per cent to about 50 per cent which is probably a significant change.

Near the end of 1949, an abrupt change in the level of noncupped fractures of Grade "B" steel was observed, as was shown by the chart (Fig. 8). The one measurable change (mentioned above as the third change) that had been made in practice coincidental with this shift in level was a change in intent for the manganese level and a slight change for the carbon level. Specifically, the intended manganese level was raised from 0.55 to 0.65 and subsequently to 0.70 per cent, while the carbon level was lowered from 0.25 to 0.23 per cent but the width of the control limits was unchanged.

The manganese-carbon ratios were calculated for several hundred heats and also their moving average values, using the same grouping as for the fractures (Fig. 8). These data were then plotted to show the relationship between the Mn/C ratio and the per cent of noncupped fractures produced (as was done for the silicon data) resulting in the graph shown in Fig. 12. A few points are included from results reported by the Melrose Park Works of the Company, on their production of the same grade of steel. As with the silicon data, after a certain period of time the correlation ceases to exist but the data shown in Fig. 12

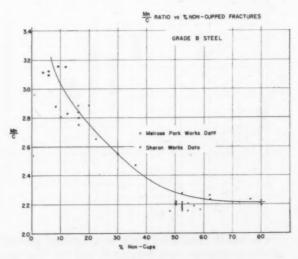
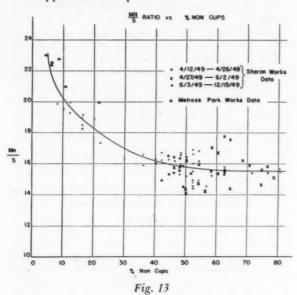


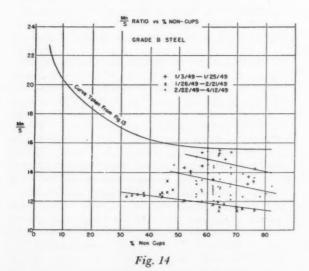
Fig. 12

represent some 350 consecutive heats, covering a period of about five months time which should be enough

data to be significant.

The relationship between the Mn/S ratio and the occurrence of cupped fractures was also determined, using the same technique as just described. In Fig. 13, the data are shown graphically for the period from April 12 to December 15, 1949, representing some 800 consecutive heats, with a limited amount of data from Melrose Park Works also included. In Fig. 14 are plotted data from three periods earlier in the year to show how the level of the Mn/S ratio in the range of 11 to 15 changed without materially affecting the level of the percentage of noncupped fractures produced. However when the ratio changed from 15 to 20 it was accompanied by a significant increase in the amount of cupped fractures produced.





The Mn/S ratio was chosen as a measure instead of the manganese itself because it was felt that the effect of variations in sulphur might thus be minimized. Sims⁴ and others long ago recognized the effect of increasing amount of sulphur on the inclusion formation and point out that "—when manganese is present in steel, it takes the lions share of the sulphur. When there is an appreciable excess of manganese over the sulphur equivalent, the sulphides that form will be primarily manganese sulphide—." The Mn/S ratio is taken as an approximation to recognize this relationship.

We do not have any formal explanation as to the mechanism by which cupped fractures should increase with an increase in either the Mn/C or Mn/S ratio. Both the carbon and the sulphur contents fluctuate within fairly narrow limits over a considerable period of time. The manganese level however, has been intentionally changed at least once (as previously noted) and even under conditions of a constant level, the scatter of results is greater for manganese than for the other elements. Consequently if the ratio of manganese and one of these more constant elements is closely connected with cupping tendency, then the ratio of manganese and any other constant element will appear to also have a correlation. It is also highly possible that the manganese itself is the important element and the ratios with the nearly constant elements may merely give a mathematically accurate correlation but one which is physically meaningless.

Herty⁵ has pointed out that when a steel is well deoxidized in the furnace, it will be much cleaner than one deoxidized in the ladle. He continues "It is my firm opinion that if the manganese specifications on most steels could be increased, the consumer would obtain a much cleaner product in a killed steel." He then goes on to point out that the manganese acts to flux the *xides* formed from the stronger deoxidizers that have been used and thus acts to clean the steel. It should be noted that in the melting practice under consideration the manganese is all added into the furnace along with FeSi and given 6 to 10 min to go into

solution before the heat is tapped.

An attempt was made to evaluate the effect of the inclusions in the steel by microscopic examination of some 50 specimens. Some of the test bars from which specimens were taken broke with cupped fractures and some broke with noncupped fractures. The specimens were examined, at 100 magnifications, at five locations on each specimen and the inclusion count noted. Attempts were made to classify the inclusions as to type of sulphide or silicate, or oxide and average values developed for the five determinations of the number of each type of inclusion for each specimen. Analysis of these data failed to reveal any correlation of the number or type of any of the group of inclusions with the cupped or noncupped fractures produced

The only indication that seemed to have any significance was that the average number of the sulphide inclusions was about 40 per cent greater in the group of specimens from heats containing 0.65 to 0.75 manganese than in those from heats of 0.55 to 0.65 manganese, although the average sulphur was only 0.035 for the first group and 0.038 for the other group. To put it another way when the Mn/S ratio was 19.5, there were about 40 per cent more sulphide inclusions

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present than when the ratio was 14.7, and the only explanation offered has to do with Herty's idea that the greater the amount of manganese present, the greater will be the amount of reaction with the deoxidation products of the stronger deoxidizer. We would again repeat that no aluminum was used in the deoxidation of these heats and consequently the amount of aluminum inclusions present is negligible.

The present investigation is not concerned with the effects of Type II inclusions on ductility and consequently on the type of fractures produced. Sims and Dahle⁴ and other investigators have adequately demonstrated these effects and it is assumed that the chain type inclusions will usually produce steel of poor ductility and the fractures will be predominantly noncupped. Practically no Type II inclusions were found in the specimens that were examined in this investigation.

Unpublished work by G. M. Guiler and J. D. Hedberg of the Research Laboratory of the Company indicates that there may be a relation between the composition of the sonims in the steel and the occurrence or absence of cupped fractures. About 16 samples were available to these investigators which represented mostly heats that had exhibited cupped fractures. The specimens were chlorinated and the residue analyzed for SiO₂, Mn₃O₄ (calculated to MnO), and FeO.

Although the results are rather few in number and represent a considerable scatter, the indications are that the ratio of FeO/MnO in the steel is lower for cupped fractures than for noncupped fractures. One group of tests gave a ratio of $\frac{\text{FeO}}{\text{MnO}} = 14$ for the

cupped and 40 for the noncupped fractures. Another group gave a ratio of 5 — for the cupped fractures.

The reaction of manganese with FeO in the steel has been that of

FeO + Mn = MnO + Fe

The more completely the FeO is reduced, the smaller the $\frac{\text{FeO}}{\text{MnO}}$ ratio should be and it probably

follows that with the increased manganese content, the farther the reaction proceeds. Perhaps this represents a further reason why the increase in manganese content produces an increase in the frequency of occurrence of cupped fractures.

One other factor that may be of importance in determining whether test bars will fracture with cupped or noncupped fractures is that of the effect of residual gases in the steel. Work on the effect of hydrogen and of the "aging" treatment on the ductility of cast steels has been recorded by numerous observers of which the work of Sims and co-workers⁶ gives perhaps the most complete picture.

For the present investigation, a lot of some 25 test results were selected in which the test bar broke with a noncupped fracture, but for which there was a du-

TABLE 3

| Yield Strength, psi | Strength | gation, | | |
|---------------------------|--|---|---|---|
| 44,900 | 76,600 | 27.4 | 41.9 | 0 |
| 45,300 | 76,800 | 30.3 | 49.4 | 71 |
| 45,390 | 77,620 | 28.3 | 43.5 | 0 |
| 45,9 2 0 | 78,330 | 28.3 | 44.8 | 70 |
| | Strength, psi 44,900 45,300 45,390 | Strength, psi Strength, psi 44,900 76,600 45,300 76,800 45,390 77,620 | Strength, psi Strength, psi per cent 44,900 76,600 27.4 45,300 76,800 30.3 45,390 77,620 28.5 | Strength, psi Strength, psi per cent per cent 44,900 76,600 27.4 41.9 45,300 76,800 30.3 49.4 45,390 77,620 28.3 43.5 |

EFFECT OF AGING TREATMENTS ON REDUCTION OF AREA

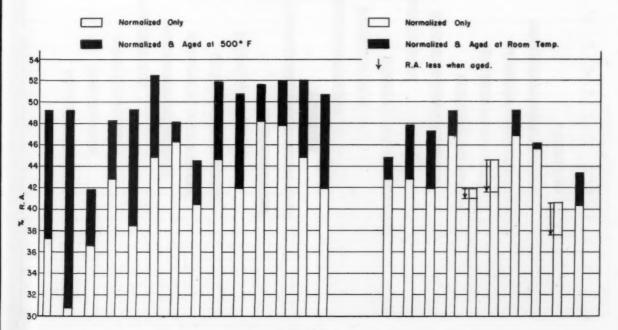


Fig. 15

plicate bar which had not been tested. All bars were of Grade "B" steel, and had been normalized only. Fifteen of the duplicate bars were given an aging treatment of 4 hr at 500 F, while the second group of ten duplicate bars were allowed to age at room temperature for about four weeks. Group averages are tabulated in Table 3.

The change in reduction of area for each pair of bars is shown graphically in Fig. 15.

Changes in tensile and yield strength, as a result of the aging treatment, are relatively minor but the change in the ductility of the group aged at 500 F is significant as is the change of the character of the fractures from 0 per cent cupped fractures to 70 per cent cupped fractures. This same change from noncupped to cupped fractures occurs even with room temperature aging.

A limited amount of data indicates that duplicate Grade "B" steel test bars, tested within a short time interval, will exhibit the same type of fracture about 80 per cent of the time. Consequently when the group of test bars was selected for study, whose companion bars exhibited noncupped fractures, the probability existed that 80 per cent of the duplicate bars would break with noncupped fractures while 20 per cent would have cupped fractures. However, since 70 per cent of the bars broke with cupped fractures after the aging treatment, the effect of the treatment would seem to be significant.

Similar results are obtained when the test bars are given a tempering treatment after normalizing. Using the same technique as before, some 25 heats were selected from which the first test broke with a non-cupped fracture and yet which was normally sound in structure. These test bars had been normalized only. The duplicate bars were normalized and then were given a tempering treatment (which consisted of holding the bars for 2 hr at 650 C). Average tensile test results are tabulated in Table 4.

TABLE 4

| Treatment | Yield Strength, psi | Ultimate Strength psi | | of Area, | Frac |
|----------------------------|---------------------------|-----------------------------|------|----------|------|
| Normalized only | 45,900 | 77,400 | 27.2 | 41.2 | 0 |
| Normalized and tempered | 46,100 | 75,300 | 31.4 | 52.0 | 80 |

Figure 16 gives a graphical comparison of the reduction of area values of normalized only tests compared with normalized and tempered tests.

The increase in ductility of the steel and the change in the character of the fractures after the tempering treatment is evident, although it has been brought about at the expense of some ultimate strength, with the yield strength remaining constant. Remembering that the first bar of each set broke with a noncupped fracture and that we would then expect some 20 per cent of the duplicate bars to break with a cupped fracture, we have an additional 60 per cent which changed to cupped fractures for a total of 80 per cent.

Comparison of the level of noncupped fractures for

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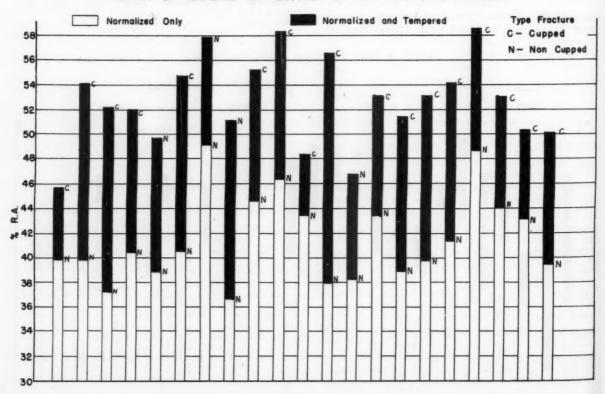
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EFFECT OF TEMPERING ON REDUCTION OF AREA AND TYPE FRACTURE



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Grade "B" steel with that for Grade "C" steel (as shown in Fig. 7, 8 and 9) will again point out that the Grade "C" steel has a consistently lower level than the Grade "B." Because the tempering treatment apparently exerts such a marked effect on the cupping tendency and because Grade "C" steel test bars are all tempered, the assumption is made that this treatment is a principal reason for the differences in level. Both grades of steel are made in the same melting units under similar conditions and are heat treated in the same furnaces so that the essential differences lie in the mild alloying and the additional tempering treatment of the Grade "C" material. Yet there is a consistent and significant difference in the type of fractures produced which may be accounted for as just discussed.

Caution should then be injected to the effect that if the level of cupped fractures is to be taken as indicative of the quality level of the melting practice, the tests should be made on bars in the normalized condition so that no corrective effects of heat treatment may be included in the results.

Since hydrogen content seems to be an important factor in influencing the tendency of test bars to break with cupped or noncupped fractures, further experimental work has been instigated in our laboratory to determine what relationship may exist between hydrogen content and physical properties of the steel. Sims and co-workers⁶ have indicated that "hydrogen in amounts of 0.0001 to 0.0004 per cent by weight is sufficient to reduce ductility to a lower limiting value of 15 to 30 per cent of the ductility possible in the hydrogen-free steel." They add, however, in the report of their work published by the Steel Founder's Society⁷ that "the hydrogen content of commercially melted steel when made without special precautions tends to a value of about 0.30 relative volume (0.00033 per cent)."

The present approach to the problem is that of establishing a method for the determination of hydrogen in the shop laboratory so that a sample of steel can be analyzed within a few hours after it is poured. The test bars from the heat can be normalized and tested also within a few hours after they are poured and the physical properties and type of fracture correlated with the amount of hydrogen present and, in turn, with melting practice, which may act to improve the quality and uniformity of the steel being produced.

The method that is being tried is that developed by Schwartz and Guiler⁸ for the determination of the hydrogen content of white cast iron. It is the direct combustion method which is alluded to in the literature for example in Scott's Standard Methods of Chemical Analysis, fourth edition. The apparatus required consists of a high temperature combustion furnace (capable of 1050 C) and combustion tube, a drying train for the oxygen supply, and an absorption U-tube filled with P₂O₅. A high grade balance is also required having a sensitivity of about 0.025 mg. A photograph of the apparatus is presented in Fig. 17.

The sample of steel is taken by immersing small vacuum sampling tubes into a spoonful of metal. Using the $\frac{1}{16}$ -in. diam tube, a pencil of metal some 4 or 5 in. long is required to weigh 6 grams. Using

this type of sample, no drilling is required and the specimen can be easily cut to the desired length.

A 6-gram sample is weighed and placed in a combustion boat and the boat inserted in the furnace. The stream of oxygen is passed over the sample for 1½ hr. The hydrogen and the oxygen react to form water which is absorbed by the P_2O_5 in the U-tube. The difference in weight between the initial and final weights of the U-tube gives the weight of moisture absorbed and the hydrogen content can be readily calculated.

The main difficulty encountered so far in our limited work has been that of thoroughly drying the oxygen so that a constant blank (largely due to the opening and closing of the furnace and introduction of the boat and sample) can be secured. Any moisture in the oxygen will be picked up in the P_2O_5 absorption Utube and included in the determination.

Details of the manipulation are available in the paper referred to above. The authors present evidence to substantiate their statement that "We believe this method to yield results similar to those obtained by vacuum fusion."

The present investigation is still in the development stage. Some 20 recent determinations have given us hydrogen values ranging from practically zero to 0.0010 per cent, with an average value of about 0.0004 per cent which is not far from the value of 0.00033 per cent suggested by Sims. All of the heats represented by the 20 determinations have been of Grade "B" analysis and the test bars from all of these heats have broken with cupped fractures when they were tested in the normalized conditions.

Much work remains to be done and, at best, this is only a progress report on this phase of the investigation.

Conclusions

The primary purpose of this discussion has been to emphasize the importance of the character of the test bar fractures (especially for normalized and normalized and tempered steel) as an indication of the quality of the steel represented by them and statistical methods have been employed to demonstrate the differences in quality levels.

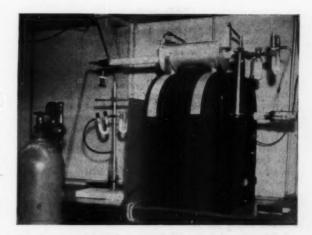


Fig. 17

Two general causes of noncupped fractures have been studied, one of which is related to the effect of gases in the steel (principally hydrogen) and which can be benefited by suitable heat treatment, and the other which is due, perhaps, to inclusions in the steel and is not benefited by heat treatment.

The effect of hydrogen is here discussed with particular reference to the increased occurrence of cupped fractures produced by such treatments as aging and tempering, since the beneficial effect of such heat treatment on the physical properties of the steel is widely recognized.

The effects of the inclusion form are indicated espe-

cially by

(a) period of overdeoxidation and attendant reduction in silicon into the metal, resulting in an increase in noncupped fractures.

(b) period of increased oxidation caused by an intentional change in the level of the melt-in carbon, resulting in a decrease in the occurrence of non-

cupped fractures. (c) period of intentional changes in the Mn/S ratio resulting in a marked decrease in the occurrence of noncupped fractures.

Some evidence is introduced to indicate the possible

causes for these observations.

A method of hydrogen determination that may be established in a foundry laboratory and provide a tool for further investigational work on the effect of hydrogen on steel is suggested. When sufficient data have been accumulated perhaps a definite correlation can be made between the type of fracture and such a variable as the hydrogen content at the time of pouring the heat.

Acknowledgment

The authors express their thanks to Dr. H. A. Schwartz for his encouragement and advice which aided so materially in preparation of the paper; to W. M. Ewing, Manager of Sharon Works, for permission to use the data and encouragement to publish them, and to numerous members of the metallurgical staff of Sharon Works including A. G. Ellis, L. H. Arner, and H. W. Ferris for their cooperation and effort in gathering and preparing the data used.

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DISCUSSION

Chairman: R. C. Wood, Minneapolis Electric Steel Castings Co., Minneapolis.

Co-Chairman: D. C. Zuege, Sivyer Steel Castings Co., Milwau-

H. H. Blosjo: 1 I notice that your cup fractures went down when your silicon content went up. This might be expected if your high silicon is due to high residual silicon early in the heat. What is the residual silicon early in the heat and how much silicon reduction do you get later in the heat? What is your silicon content, for instance, at the melt down and what is your silicon content just before you make your silicon addition?

Mr. Johnson: We do not know exactly what the silicon content is at melt down but we think it is perhaps 0.20 or 0.25 per cent. We do try to have a sufficiently vigorous boil so that the silicon is reduced to 0.07 per cent or less. We think when we stay up around 0.10 to 0.14 per cent that such values indicate

that we have not had a sufficiently good boil.

MR. BLOSJO: I am a bit surprised because silicon is around 0.05 to 0.08 per cent in the melt down in our case with a black slag containing approximately 35 per cent FeO. As we boil we pick up silicon. So at tapping time, without any addition, we would put on 20 or 25 points of silicon.

Mr. Johnson: You purposely do that do you not?

MR. BLosjo: Yes, we purposely do that. We have not picked up as much silicon in recent years as we used to. This is due to the fact that we have increased our power input, thus reducing the time the heat is at high temperature. We, however, start to reduce silicon almost as soon as the boil starts. Very slowly at first, but increasing in rate as the metal temperature goes up and the FeO content of the slag goes down.

MR. JOHNSON: I am fairly sure of the 0.06 and 0.07 values that I quoted because we frequently check on the silicon level by taking a test after the boil, before any additions have been made to see if they have had what we consider an adequate boil. The few tests that we have run would indicate that we probably pick up to probably 0.12 to 0.15 per cent silicon during the course of holding the heat before we put the silicon addition in.

MR. BLOSJO: What is the FeO content of the slag when the

silicon is 0.06 or 0.07 per cent?

MR. JOHNSON: It is about 35 per cent. It has to be high with the silicon down.

W. S. Pellini: 2 What was the size of the casting which was sectioned for the test bars of your first tests? Was it a fairly large casting? If so, was the reheat-treatment performed on the same section? You show an increase in reduction of area as the result of the normalizing reheat-treatment. Proper evaluation of the effect of the reheat-treatment depends on the relative section sizes used for the two heat treatments.

MR. JOHNSON: On the tests themselves?

MR. PELLINI: It is not clear if the first values reported represent heat treatments of the same sections as the second group of values. Was the second group of specimens machined from reheat-treated castings or from small slags cut from the castings prior to heat treatment?

MR. JOHNSON: Those were the conventional keel blocks.

Mr. Pellini: Were these renormalized as such? Did you heat treat the entire keel block or was the keel block sectioned into slabs prior to heat treatment?

Mr. Johnson: I think we knocked those tests off before the

aging experiment.

Mr. Pellini: It should be expected, because of the alloying effects of the manganese, that a different and finer pearlitic structure would be developed on normalizing the smaller cut sections. The increased ductility noted may be due only to improvement in microstructures resulting from the more rapid cooling of the smaller sections.

MEMBER: Did you resort to titanium for the purpose of in-

creasing the ductility?

Mr. Johnson: Yes, we do not want to have to temper our Grade B steel. We just want to normalize it and have it meet the specifications. We switched to titanium boron in order to be sure we stayed away from Type II inclusions which might require tempering some of the material. We have a continuous flow of metal going through and we do not want to have to segregate it and do any special heat treatment on it. We find that we get sufficient deoxidation for our class of work from the titanium-boron addition.

¹ Metallurgist, Minneapolis Electric Steel Castings Co., Minneapolis.

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Мемвек: Have you noted any pickup of titanium in your steel when it was finished?

MR. JOHNSON: On the basis of a limited number of analyses, about 0.0045 per cent soluble titanium and 0.0065 per cent insoluble titanium may be present in the steel.

MEMBER: What percentage was added?
MR. JOHNSON: We added 5 lb of titanium-boron per ton or about 0.04 per cent titanium is added.

MEMBER: Is that normalized?

Mr. JOHNSON: Yes, it is normalized.

C. L. FORNWALT: 8 In regard to your manganese sulphur ratio how high did you go in sulphur and do high sulphur contents have any effect on your cup and non-cup test bar?

Mr. Johnson: Our sulphur ratio is 0.035 for one group and 0.03 average for the other. The sulphur is just that found in ordinary acid electric practice. It will range from 0.030 to 0.045 per cent.

MR. FORNWALT: Would it have any effect on the test bars

when you go up to 0.045 per cent?

Mr. JOHNSON: I do not think it will. Usually we do not go much over 0.040 in ordinary acid practice.

⁸ Head, Metal Processing Branch, Naval Research Laboratory, Washington, D. C.

⁸ Superintendent, Atlantic Foundry Co., Akron, Ohio.

MATERIAL TRANSPORT IN THE CLEANING ROOM

By

N. L. Smith1 and R. J. Wolf2

This paper is intended to treat briefly the problems incidental to the choice and use of equipment in the cleaning room. By "material transport," is meant movement of products of the foundry, rather than of materials that have to do with operations on foundry products. This paper covers the several types of material moving units which are in common use. An endeavor will be made to show the factors which have a bearing on the choice of equipment; the several types of such equipment with brief sketches of their construction; their uses, advantages and possible limitations. The paper is not intended to set up criterions, rules or formulae, but rather to act as a guide. In the main, the thoughts have been gained from past experience. Conditions are different in most respects in every plant, and it would be impossible to state rules which would govern all cases.

Factors Influencing General Layout

Too many times foundry mechanization has been thought of as pertaining to those departments which deal mainly with actual production of castings; the charging yard, charge make-up, melting, pouring, molding and sand preparation departments which are those on which we usually concentrate. This having been done, a bottleneck may be created somewhere else along the line which frequently does not show up until later. At times it is in the cleaning room. The bottleneck may be the machines which actually perform the operations of the cleaning room, but can be a result of the layout of those machines and the methods used in handling work to and from them. Consequently, it is good practice to include the cleaning room, and possibly other departments in the overall plan of foundry plant improvement.

Product—The product of the plant itself will, to a large extent, determine the class of handling equipment chosen. The processes to which that product must be subjected will determine certain locations of equipment, specified work spaces, paths of travel, dis-

positions of related equipment and freedom of movement of personnel.

Arrangement of Related Departments—Care should be used in the planning stage to investigate positioning of departments before and after progress into or through the cleaning room. Paths of travel may be converging into one another or crossing. Should free movement be impeded, obviously a bottleneck has developed. Think also of the future when capacities may be over and above those now needed.

Work Space—Needless to say, the physical aspects of the foundry property play an important part in the choice of equipment. This is true in plants that have gathered considerable age, for here are often found buildings and operations that have grown like "Topsy," and rearrangement may become a major problem. Plants erected years ago, when structures did not have the advantages gained from modern materials were frequently laid out in numbers of small bays with low headroom. Thus, along with the choice of materials, there is the necessity of building changes to gain all the advantages accruing with the introduction of materials handling and labor aiding devices or a compromise is necessary in selecting the equipment.

Location of Existing Equipment—Existing positions of cleaning room equipment may, as has been suggested in the previous paragraph, have as large a bearing on the choice of handling units as the building itself. Some machines which require pits may have to stay "put" and this offers additional complications. At times it pays to sacrifice such construction to gain improved handling. It has been found, in numerous cases, that the purchase and erection of newer cleaning room devices was made with little reference to future equipment needs. Because a space is available that seems large enough for the machine and its operator, there we plant it, only to regret our decision for years afterwards. Such situations often mean, instead of choosing the most suitable material transfer units, the next best must suffice. Regardless of whatever has gone before in the matter of equipment and its placement, the problem now is how far should we go in discarding, tearing out and rearranging for the new order of things.

¹ Link-Belt Co., Philadelphia.

² Stone & Webster Engineering Corp., Boston.

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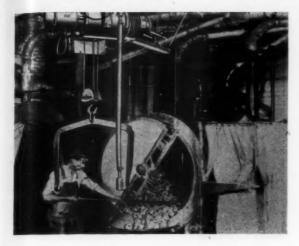


Fig. 1-Monorail with electric hoist handling castings in and out of blast cleaning unit.

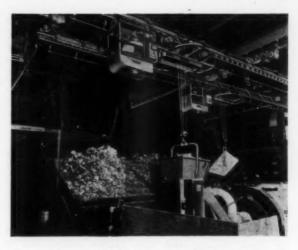


Fig. 2-Long span light duty crane with operator's cab distributing castings in cleaning room.



Fig. 3-Multiple light duty cranes with crossovers to give full coverage in cleaning room.

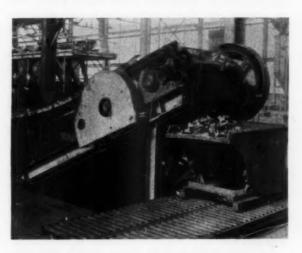


Fig. 4-Roller conveyor with tote box for sprues dumping without removal from lift truck.

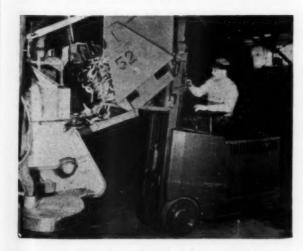


Fig. 5-Charging blast cleaning unit with fork lift truck.



Fig. 6-Discharge from blast cleaning unit to self dumping fork lift truck container.

Elevation of Buildings or Equipment—This item also has a direct bearing on the choice of handling units. Some conveyors or material movers are limited in their angle of rise, which preclude their use under these conditions. It may happen that a combination of two or more units is required to make the necessary move. This may mean sacrificing an ideal situation and substituting one of a second order.

Description of Mechanical Equipment

We believe that all foundrymen have a general idea of the construction of the several pieces of equipment mentioned. However, to clarify our remarks, we are giving a brief outline of the essential parts.

Belt Conveyors

Carrying Medium—A rubber belt having a carcass of duck, impregnated with rubber compounds of various analysis. Similar duck carcasses are also impregnated with heat resisting compounds to suit certain conditions. Additional covers can be added, such as asbestos, etc. Other belts exist having wires embedded in their structure, but these are for loads and pulls never found in cleaning room service.

Wire belts are used in places, but they require a clean product and it may be safe to say their use within

a cleaning room is rare.

Supporting units — Generally antifraction rolls, termed idlers. Tables of wood or steel are also used depending upon the problem.

Terminals—These are cast or fabricated steel pulleys of diameters to suit conditions. They may be plain or covered with rubber to provide added traction.

Drives—These are the same type as may be found in any other power conveying unit, and generally consist of a motor, and speed-reducer unit connected either directly or by a chain drive to the head pulley.

Apron Conveyors

Carrying medium—Steel plates, straight and plain or corrugated, mounted on one or two chains in pitches of 4 in., 6 in., etc. Chains may slide, or they may roll. Generally the roller is a part of the chain and in contrast with the belt conveyor, becomes the supporting unit.

Terminals—Cast sprockets, the periphery of which is made to suit the chain being carried. Variations of course exist, to suit the conveyor being used.

Drives—The same as has been suggested in the preceding paragraph.

Oscillating Conveyors

Carrying medium—A single length of steel plate carried on side structural members, forming a one-piece trough the length of the conveyor, or made into two or more identical units, should the length of the conveyor demand such treatment.

Supporting units—Mostly angularly placed legs attached in suitably constructed joints to both the trough and the steel members on the floor forming a base

Terminals—In contrast to the two preceding conveyors, this is generally a single group placed centrally or in some cases at either end of the conveyor. Is in the form of an eccentric or crankshaft and provides an oscillating motion to the trough.

Trolley Conveyors

Carrying medium—A two or four-wheel trolley carrying the load by suspension hook, or possibly two trolleys connected by a bar with the imposed load carried centrally and towed by a conveyor chain in a predetermined path.

Supporting unit—An I-beam track bent into horizontal and vertical curves interspersed with straight sections, all arranged to suit the peculiar conditions

under consideration.

Terminals—The unit, corresponding to the terminals of belt or apron conveyors, is generally a caterpillar drive.

Roller Conveyors

Carrying medium—Tubes containing antifriction bearing turning on stationary shafts. The tubes are closely spaced to suit conditions.

Supporting units-Structural shapes, punched to receive the foregoing shafts, may be solidly carried on

supports, or spring mounted.

Drives—Quite generally, roller conveyor is a manually or gravity operated conveyor; however, power may be added to provide elevation or to assist in moving loads.

Monorail, Light Duty Cranes and Lift Trucks

For the foundry, which because of size, wide range of product, available floor space or other reasons, the use of chain or other types conveyors is not suited nor economically justified, there are available power driven lift trucks, monorails and light cranes. (Fig. 1 to 3.) These, used singly or in combination with each other or with conveyors can materially improve material handling in the cleaning room.

The range of these products and their use is so large that with proper analysis of the handling problem in the cleaning room, the selection of simple combination of one or more types of standard units can usually be worked out to effectively use the available floor space and increase the production of personnel

and equipment.

Gasoline or electric driven lift or fork trucks are well suited for handling miscellaneous lots of castings in the cleaning room, as well as other handling problems in the foundry. The fork lift truck offers several advantages over the platform lift truck including higher lift and ability to pick up large castings directly off the floor and deposit them on skids or work benches. However, as they have no bed, pallets or boxes must be provided for small work.

Boxes for either type of truck should be provided with lifting eyes where crane or monorail service is also available so that they can be readily handled by

overhead equipment.

The combination of trucks and monorails or cranes provided great flexibility in handling in the cleaning area, and is recommended where required due to the weight of the castings. Where the castings are light, it is possible to achieve with a lift truck substantially all that can be accomplished by a combination of trucks

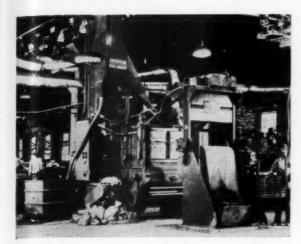


Fig. 7-Combination monorail, lift truck, tote boxes and wheeled containers.

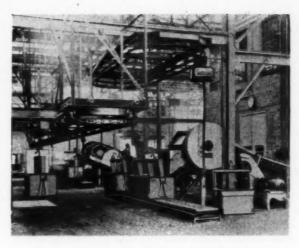


Fig. 8-Combination apron conveyor, roller conveyor, electric hoist on monorail and trolley conveyor.



Fig. 9-Combination apron conveyor and wheeled containers hauled by fork lift truck.

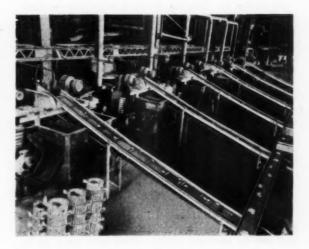


Fig. 10-Belt conveyors handling castings to and from grinders.

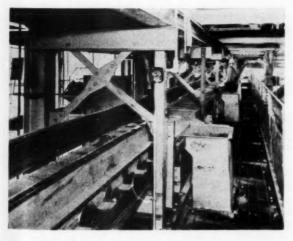


Fig. 11-Apron conveyors handling castings to sand blast machines on lower floor.



Fig. 12-Apron conveyor between two elevations handling castings from shake-out.

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the t, it all and cranes or monorails. Where only lift trucks are used, the boxes should be of the quick dumping type which permit unloading the castings into cleaning units or on work benches without any manual han-

dling (Fig. 4).

Where the individual casting weight requires mechanical handling, use of floor operated cranes designed of special monorail sections offers great flexibility. These units can be either manually or electrically operated in any or all movements. Where the area covered is not too great, air hoists can be used effectively. This equipment is made in a wide range of standard sizes which can be readily combined to meet almost any condition. The cranes, monorail and crane rails are made with a flat tread which reduces the power required to move the load. The joints are designed to eliminate irregularities thus providing smooth easy travel. With this type of crane the entire floor space can be serviced and crossovers make it possible to transfer a load from a monorail to a crane or between two cranes running on parallel runways.

In setting up cleaning units, it is well to set their elevation so that the castings can be dumped from the boxes directly into the unit and the cleaned castings discharged directly into the boxes without the necessity of manual handling. See Fig. 5 and 6. This can be readily accomplished with forethought and planning.

Jib cranes have a more limited use but prove effective in some areas. Swing grinders have considerably more flexibility when suspended from jib cranes or monorails.

In foundries that desire to install materials handling units, it is possible a combination of several types of handling equipment will be chosen. Each work station becomes a separate problem. (Fig. 7, 8 and 9.)

Factors Influencing Choice of Equipment

Temperature of Materials Handled—This condition must be observed carefully. Castings to be cleaned and are at elevated temperatures may have to be handled on conveyors of the steel bottom type, in tote boxes or other such carriers. Perhaps the belt conveyor could do the most direct job, with the least manual effort to service, but the belt would be destroyed in a short while from the heat of its load. On the other hand, it is possible the heat is within the safe range for handling on the belt. It is best to be sure on this point, before a definite choice is made.

Size and Character of Materials Handled—This point goes hand in hand with the heat situation. Castings of large sizes may compel the choice of a certain type transfer medium. It may be that although castings are of the same size, certain other characteristics make same type of mover wholly unsuitable. Small fragile castings may be moving into the cleaning room at the same time as larger ones, and a system must be chosen that will adequately handle and protect all of them.

Cost—Cost of the installation and its operating expense are the final answer to the problem. As mentioned before, it is possible the initial cost of the equipment does not tell the whole story. The equipment chosen may be initially more expensive than thought necessary or expedient, and yet when the

various surrounding costs are totaled it may prove in the end to be the cheapest investment. The best plan for making this decision is to examine the initial and operating costs of the various types of movers available at the time that engineering is in progress.

Equipment Advantages and Limitations

Assume the layout can provide a straight line operation, it is quite possible a belt (Fig. 10) or an apron conveyor (Fig. 11) should be chosen. Should excessive heat be present, the belt conveyor may be ruled out in favor of the apron. Where the castings are of excessive size, or operations verging on the violent must be performed, an apron will be the choice. In a straight line run where a considerable rise in elevation is necessary (Fig. 12), an apron may also be the choice or possibly a combination of a belt and an apron may be chosen.

Picking, sorting operations and despruing can be performed on either a belt or apron conveyor (Fig. 13 and 14) and both have been used successfully.

For receiving castings delivered from a cleaning machine operating as a batch unit, it is quite possible that an apron will be better suited to receive the shock load than a belt.

The oscillating conveyor is also being received favorably and operations that have been cited for an apron can also be performed on an oscillator (Fig. 15).

Where magnetic removal is necessary, a belt conveyor may work in to better advantage, since the discharge pulley can be of the magnetic type. To use an apron under similar circumstances will require a stronger magnet on its head shaft than is required with a belt conveyor. With either a belt or apron conveyor, the magnet can be suspended above the conveyor and can be so arranged as to convey the pickup to one side for automatic discharge.

A trolley conveyor can also be arranged to serve in cases where conditions indicate an apron. Starting at the shake-out, it can receive hot castings, either manually or automatically. While this is outside the discussion of the cleaning room, units of this type can become a part of the cleaning room procedure. They are often used to carry the castings through a path in or outside of the building for a specified length of time, as required to cool them before passing into the cleaning room for subsequent operations.

Discharge from the trolley conveyor can be as automatic as receiving and effected at any point desired.

Trolley conveyor is flexible and can pass through any path or occupy a space above the workers, thus leave floor area free (Fig. 16).

The trolley conveyor can be made of considerable length with varied paths horizontally and vertically (Fig. 17 and 18). It can discharge to one operation and pick up from the next succeeding operation.

Roller conveyors can be made rather flexible also, although they may need some auxiliary functions to make them approximate the trolley. They are admirably suited to batch operations (Fig. 19), and for intermittent operations. They can be provided with switches or turn-outs for spotting loads to any worker or operation desired. They can be used as feeders to other types of conveying equipment. Their use has been universally accepted, and no doubt they may be

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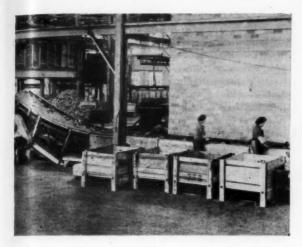


Fig. 13-Spruing and sorting on an apron conveyor.



Fig. 14-A picking apron conveyor.

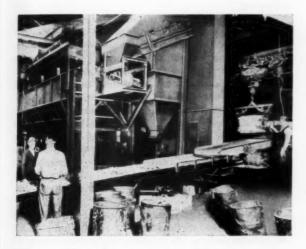


Fig. 15-Picking and sorting on oscillating conveyor.



Fig. 16-Using space over workers for material transfer.



Fig. 17-Illustrating varied path for trolley conveyor.

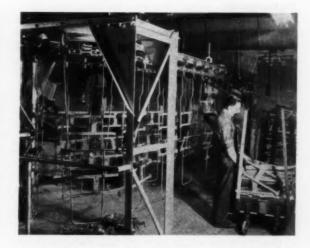


Fig. 18—Change in path horizontally for trolley conveyor.



Fig. 19-Box on roller conveyor receiving batch from cleaning machine. Note car section in the roller conveyor line.

found in far more places than most any other type of conveyor. It goes without saying the use of roller conveyors entails the use of tote boxes (Fig. 4) or other type of containers for small or nominal sized castings. Large castings, depending on form, can be handled on roller conveyors without boxes.

Comparative Costs

Cost comparisons are difficult to make because of the many variables that enter into them. To state a per foot cost is also rather misleading. The cost of drives must be prorated over the footage, and slower speed machines with large reductions will show increased costs over a machine of the same centers having higher speeds.

Generally speaking, if we choose power conveyors of the same length for comparison, costs will appear from the cheapest to the most expensive in the following order:

1. Belt conveyors

2. Oscillating conveyors

3. Apron conveyors.

Maintenance costs may run in the same order. Good housekeeping has a direct bearing and if the loss of production or "down-time" is charged against maintenance, preventive maintenance will show economies and should be carefully studied. Preventive maintenance does not always seem possible, but it can be practiced to some degree and will give a good account of itself on the cost sheets.

Summary

It has been suggested elsewhere in this paper that it is impossible to lay down a hard and fast rule for the choice of handling units. This applies also to departments other than the cleaning room, and indeed to other industries. Conditions vary to such an extent that good judgment and costs must be the controlling factors. Two situations that seem to have the same conditions may differ widely in their final solution. The closest that one can come to rules might be the following.

1. If straight line operation can be obtained between two points and operations are uniform, use a floor-mounted belt or apron-type conveyor. This statement of course includes other conveyors that bear the same general form or function as those mentioned. Again, modification is necessary since roller conveyors and trolley conveyors will and do perform the same function under the conditions mentioned.

2. If the operations are uniform and the sequence of operations fixed but the locations of succeeding machines scattered in a pattern other than a straight line, the trolley-type conveyor can be profitably selected. This statement must also be qualified as monorail conveyor can be employed and may be better suited.

3. If neither operations nor path present a definite pattern, the best method may be the use of portable equipment such as lift trucks or light duty cranes.

Each foundry has its own peculiar handling problem and no hard and fast rules can be set for solving them; however, careful study and planning can produce an economical solution.

Acknowledgment

Photographs were furnished by Mathews Conveyor Co., The Cleveland Crane & Engineering Co., The Yale & Towne Mfg. Co., and Link-Belt Co.

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EFFECT OF MOISTURE ON CORE SAND MIXTURES

By
O. Jay Myers*

FOUNDRYMEN REALIZE the profound effect moisture has on their core sand mixtures.

The Foundry Research Laboratory of The Werner G. Smith Co. has undertaken this extensive series of tests in order to investigate the reactions which occur in the physical properties of cores when the moisture content of the sand is varied.

Why is moisture included in nearly all core sand

Undoubtedly, water was first employed because it occurred naturally in the sand from which cores were to be made; however, the fundamental reason for using this "binder" was its inherent tendency to give the core green bond.

Use of dry sand as the starting point for controlled core sand mixtures is relatively new but a practice rapidly gaining in popularity. It is our hope that data shown in this report will aid in furthering this method of core sand preparation.

There appears to be one valid reason for using a moist sand as the basic material for a core sand formulation. Perfectly dry sand segregates during handling unless a single grain size material (A.F.S. Standard 50-70 Testing Sand) is used. This means that, with a commercial dry sand, each batch has a slightly (usually insignificant) different grain distribution. The more a dry sand is handled and transported, the more it is liable to segregate. A moist sand, on the other hand, has enough cohesiveness (green strength) between the sand grains to inhibit this segregation.

It is our opinion that the lack of control of core sand mixtures made from incoming wet sand is a more serious deficiency than the segregation that possibly would be associated with dried sand. As this paper will show, lack of moisture control can be a serious matter—especially if it gets out of bounds.

Materials Used

The sand chosen for this test was a typical Michigan Lake Sand having the screen analysis shown in Table l.

TABLE 1—SCREEN ANALYSIS AND A.F.S. SAND PROPERTIES

| U. S. Series No. | Per Cent Retained |
|--|-------------------|
| 30 | 0.2 |
| 40 | 4.3 |
| 50 | 21.9 |
| 70 | 48.9 |
| 100 | 22.5 |
| 140 | 2.0 |
| 200 | 0.2 |
| 270 | _ |
| Pan | _ |
| Total | 100.0 |
| Per Cent Fines | 0.2 |
| Per Cent A.F.S. Clay | 0.5 |
| Grain Fineness No. | 52 |
| Base Permeability Number | 163 |
| Density of Sand (as aggregate) | 1.55 gm/cc |
| Density of Sand (as mineral-SiO ₀) | 2.68 gm/cc |

Lake sand was selected because it represents the normal type of sand used by some of the largest foundries in this country. It is the most economical fine aggregate available to them and can be used to found all types of metals and alloys.

Segregation of Sands

We attempted to overcome undue segregation of this lake sand by drawing relatively small (25-lb) batches of sand from the bottom of a 2500-lb capacity bin, hence we were certain of obtaining a uniform product. Approximately half a ton of this sand was used for these tests.

Since the sand employed in any core sand mixture is the most important part of the core (usually representing at least 90 per cent of its weight), a study of this fundamental ingredient is imperative.

Previous studies have shown that the physical properties of the base core sand are much more important than its chemical properties.

Of all the physical characteristics, the surface of the sand (its condition and area) is the most significant point to consider.

Table 2 shows the calculation of the surface area of the sand used in this test. (These data are based on the assumption that the sand grains are spherical.)

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⁶ Director of Foundry Research, The Werner G. Smith Co., Minneapolis, Minn.

TABLE 2-SURFACE AREA Lake (Dune) Sand

| U.S. Series No. | Surface Area (sq cm) per gm of sand | Per Cent Retained (gm per hundred) | |
|--------------------|--|---------------------------------------|--------|
| 30 | 31.34 | 0.2 | 6.3 |
| 40 | 44.38 | 4.3 | 310.7 |
| 50 | 62.51 | 21.9 | 1370.0 |
| 70 | 88.40 | 48.9 | 4322.8 |
| 100 | 124.85 | 22.5 | 2808.1 |
| 140 | 176.47 | 2.0 | 352.9 |
| 200 | 250.39 | 0.2 | 50.1 |
| Total Surf | ace (sq cm) per 100 | grams of sand | 9220.9 |

Digressing a moment, a rapid calculation from Table 2 indicates that normal 2000-lb batch of core sand contains 90,000 sq ft of surface area (or more than two acres)!

In a normal production mixture, 1 per cent (or 20 lb) of core oil would ordinarily be used as the bonding agent. Using a density of the core oil as 0.94 (gr per cc), mathematics show that the thickness of oil film is approximately one micron (or 1/100th of the thickness of an average human hair!). Moistureless cores attain tensile values in excess of 200 psi with this thin an oil film!

Microscopically, the sand appears subangular with the angularity increasing as the sand becomes finer. The material is relatively homogeneous and clean with no excessive dirt or dust clinging to the sand surfaces.

Binders—The two binders employed were stock core oil and cereal binder.

Equipment—All of the core sand was mixed in a 24-in. diam laboratory, muller type mixer.

Standard A.F.S. instruments were used for forming and testing the cores.

The cores were baked in a gas-fired, core oven.

Before testing, the briquettes were thoroughly cooled in laboratory desiccators.

Type of Tests—Four distinct types of mixtures were made for this study:

- 1. Sand and water
- 2. Sand, water and core oil
- 3. Sand, water and cereal binder
- 4. Sand, water, core oil and cereal binder

Mixing—The first test (in which sand and water alone were used) was mixed in the following manner:

Twenty-five pounds of dried lake sand was placed in the muller and spread evenly over the bottom of the pan. Two hundred cc's of water (1.7 per cent) was added to the sand and the batch was mulled 3 min (with the cover on the mixer). Several samples were withdrawn and tested for apparent density, green permeability and green compression. The tested sand was quickly returned to the covered muller.

An additional 200 cc's of water was added to the mixer and the batch was mulled two more minutes, after which, tests were made as cited before.

Water was added in 200-cc increments, and the sand processed as explained above until no more water could be added without "free" water appearing on the pedestal cup under the rammed, cylindrical, A.F.S. specimen. This "liquid limit" occurred at 1800 cc's of water (13.7 per cent calculated).

The question of mixing times naturally arises when one batch of sand is used and mulled for extended lengths of time. Points along the physical property-moisture curve were checked by mulling single batches of sand for lesser mixing times at various moisture levels. There was no appreciable difference in test results over that made with the single batch of sand.

The second series of tests were made with 25-lb batches of lake sand and 1 per cent core oil by weight. The moisture was varied as follows:

Mixtures were made using no moisture at all as well as with 50, 100, 200, 400, 600, 800, 1000, and 1200 cc's of water.

When water was used, the sand was first mixed with the water for 3 min after which the core oil was added for a 6-min mixing cycle. The moistureless sand was conditioned with core oil for 6 min total mulling time.

The third series of tests was performed with cereal as the sole binder. The dry ingredients were mulled for 1 min, followed by the same moisture additions as in the previous tests. The wet sand was mixed for three additional minutes in each case.

The practicability of core sand mixtures with cereal or water as the sole binder is open to question. These tests were performed with the view in mind of determining how much value could be attributed to these binders when used by themselves so that they could be compared to mixtures formulated with combinations of them.

The last series of tests was performed on a practical core sand mixture containing both cereal and core oil as green and dry bond additives respectively.

The ingredients were added in the following manner:

Sand, cereal binder, water, core oil. The sand and cereal were mixed for 1 min, the water was added and mulled for 3 min and finally the core oil was added for an additional 6-min mixing cycle.

Procedures

Normal A.F.S. procedures were used whenever applicable. The aggregate density values were obtained from triple-rammed A.F.S. cylindrical (2-in.) specimens. The permeability readings were made on a manual Dietert A.F.S. Permeability Meter.

Compressive strengths represent the average of at least three individually weighed specimens for each reading made on a Saeger (3.0 psi max) Green Compression Tester.

At least ten A.F.S. tensile briquettes were weighed, formed, rammed, baked, and tested for each baking period of 45, 75, 105 and 135 min each. The cores were placed five to a 10-in. x 14-in. x ½-in. transite plate in a staggered position in the core oven. Forty briquettes were placed in the oven for each load, together with a hand-rammed 4-in. x 4-in. x 4-in. block core. The oven was heated 50 F above the baking temperature to make up for the heat loss encountered during loading. All cores were baked at 425 F.

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The block cores were fractured vertically immediately after withdrawal from the oven and the unbaked (green) sand was scooped from their interiors.

The "per cent baked" represents the weight of the

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baked (unscooped) portion, divided by the total weight of the baked core.

Hardness traverses were made on the outside surfaces of the block cores where long scratches made fairly consistent readings possible.

Previous work had shown what baking cycles were proper for the bakability tests. Seventy-five minutes was used for the oil alone and 45 min when cereal was included in the mixtures.

During the performance of this experiment, it was possible for us to check our procedures. When oil was the sole binder, we checked the water-sand mixtures (specific gravity, permeability, green bond) before oil was added. When core oil and cereal binder were used together, we were able to check the previous "cereal only" results before the core oil was added. Remarkably consistent results were obtained despite the use of different batches of sand and different meteorological conditions each day.

Records were kept of the relative humidity and room temperature during the entire series of tests. No correlation could be detected between these data and the test results.

Since this experiment was performed to investigate the effect of moisture on the various core sand mixtures, exact amounts of water were added to the batches of sand. Table 3 gives the actual cc's of water added, together with the calculated percentage of moisture for each mixture.

TABLE 3-MOISTURE ADDITIONS

| Actual Water added (cc's) (per 25 lb of sand) | Calculated Per Cent Moisture |
|--|---------------------------------|
| 0 | 0.0 |
| 50 | 0.4 |
| 100 | 0.9 |
| 200 | 1.7 |
| 400 | 3.4 |
| 600 | 5.0 |
| 800 | 6.5 |
| 1000 | 8.0 |
| 1200 | 9.5 |
| 1400 | 11.0 |

Rapid moisture tests made in a "bomb-type" tester showed the water content to be the same as the calculated moisture when the moisture was less than 6.5 per cent. Above this calculated value, the bomb tester was much less accurate, probably due to the lesser amount of sand used for the test. Oven drying tests were also performed to determine actual moisture contents.

All binders were added to the mixtures by weight. The core oil was weighed into a tared beaker whose sides had been previously wet with the core oil. Post-taring, after admitting the 113.5 gr of core oil into the 25 lb mixture, assured a constant 100:1 sand:core oil ratio.

Data

A minute examination of the data which accompanies this report is tedious, but necessary to form valid conclusions.

This section of the bulletin will be divided into the various types of tests performed and the interpretation of the results obtained therefrom.

Density—The term "aggregate density" will be used for the weight per unit volume of uniformly triply rammed, consolidated core sand mixtures.

Although the green aggregate density appears to be markedly affected by the amount of moisture in the core sand mixture, the density of the final baked core is but little changed by the initial moisture.

The effect of moisture on this apparent density of the core sand mixture is to spread the grains of sand apart. After the grains have stabilized their positions in the core, their interstices are flooded with excess water. After baking, the effect of the excess interstitial moisture is lost.

Since all of the tests were performed on standard A.F.S. specimens of a known weight, and since the exact weight of the ingredients added to these mixtures were known, the aggregate densities of the baked core could be calculated. Further, this weight was checked by reweighing a baked permeability specimen. Our results indicated that:

1. The densest baked core sand mixture which can be made (using the ingredients cited in this report) is one where sand and core oil are the sole original constituents of the mixture.

Sand mixtures containing core oil are denser than those where no oil is used.

The reasons for the above results may be explained, by considering the core oil to be a lubricant as well as binder.

The sand grains are packed more closely together during ramming when core oil is present than when it is absent.

The presence of cereal binders tends to reduce the density by: (1) inhibiting flowability; and (2) by the physical presence of such a comparatively light weight material. The reduction in flowability is partially overcome when core oil is included in the mixture.

Permeability—As may be expected, the green permeability properties of a core has an inverse relationship to the density. As the moisture in a green core sand mixture goes up, the apparent density increases and the green permeability decreases. Filling of interstitial voids adequately explains this phenomenon.

In reviewing these tests, it is also apparent that the more binder in a core sand mixture, the less is the permeability. This, again, may be attributed to void filling.

Certain facts about permeability, as suggested by this test, are listed herewith:

- 1. Moistureless sands are less permeable than those containing water.
- 2. Increasing the amount of water first increases, and then decreases the permeability of a green core sand mixture. The base permeability of a sand is always less than if it contained just the right amount of water to spread the grains apart through surface action. In this case, the base permeability number is approximately 160 and the addition of merely one to two per cent water raises the permeability to more than 230.
- 3. Core oil, through its lubricating (and attendant packing) phenomenon, provides a core with somewhat less permeability than if just sand and water were used.

4. Cereal binder, due to its "stiffening up" of the sand, affords an increase in the permeability of the core. This increase is probably indirectly related to

the flowability of the mixture.

5. Although the cereal binder ostensibly "opens up" a sand (due to its resistance to flow during ramming), the inclusion of a core oil in a mixture aids in overcoming this resistance. A "Green Permeability-Per Cent Moisture Curve" of a combination of oil and cereal would closely follow that curve described by the oil itself.

6. Increasing the amounts of binder in a core sand mixture decreases the green permeability of the sand by filling the voids between the sand grains.

7. Since cores are used in the foundry in a baked condition, the baked permeability is much more significant than the green permeability. This baked permeability of a core sand mixture is usually greater than the green permeability due to the removal of the interstitial moisture and volatile ingredients during the baking process.

8. The difference between the baked and green permeability of an oil-bonded sand mixture closely describes a similar curve to the aggregate density of the mixture (when plotted against per cent moisture).

9. Baked permeabilities of oil-cereal bonded mixtures are lower than if either binder is used separately. More complete interstitial void filling probably accounts for this.

Green Strength—The green bond of any core sand mixture determines its ability to be formed into cores and to withstand handling prior to baking. Although this physical property is transient, its control is imperative to assure sound cores having dimensional stability.

The part played by the sand alone in producing green bond is not well recognized. The role played by sand and water in giving the cores green strength is far underestimated.

A comparison of the green bond effects of various additives are depicted in Table 4:

TABLE 4-GREEN COMPRESSIVE STRENGTH, PSI

| H,O, | Water Only | 1% Core Oil | 1% Cereal | 1% Core Oil 1% Cereal |
|------|---------------|-------------|-----------|--------------------------|
| 0.0 | 0.0 | 0.1 | 0.0 | 0.2 |
| 0.4 | 0.0 | 0.1 | 0.0 | 0.3 |
| 0.9 | 0.0 | 0.3 | 1.0 | 0.7 |
| 1.7 | 0.4 | 0.3 | 0.8 | 0.7 |
| 3.4 | 0.5 | 0.3 | 0.7 | 0.6 |
| 5.0 | 0.6 | 0.3 | 0.6 | 0.6 |
| 6.5 | 0.6 | 0.4 | 0.6 | 0.5 |
| 8.0 | 0.7 | 0.4 | 0.6 | 0.5 |
| 9.5 | 0.8 | 0.5 | 0.6 | 0.5 |

Before this table be analyzed, the importance of the sand must be reiterated. The use of a finer sand will change these data appreciably and not necessarily in the same direction.

When Lake (dune) Sand is used, the following conclusions may be drawn:

- 1. The most important, practical, green bond additive to a core sand mixture is the cereal binder.
 - 2. Moisture itself affords appreciable green bond to

a sand-water mixture after the grains have been properly wet (above 1.0 per cent moisture).

3. Core oil as the sole binder adds beneficial green strength to a sand only in the extremely low moisture ranges (up to 1 per cent).

4. The addition of more than 5 per cent water to a cereal-bonded mixture does not increase the green

strength.

5. Core oil depresses the green bond of a cereal bonded core.

A combination of core oil and cereal is the only tested mixture which will give workable green bonds throughout the entire moisture range.

7. At extremely high base sand moisture (9 per cent), the addition of either cereal or oil will reduce the green strength. Lubrication, with attendant slippage between the sand grains, accounts for this.

Baked Tensile Strength—The baked strength of a core is important. Together with hardness, this property determines how the core can be handled after it is prepared. Unless the sand mass is strong enough, hooks will pull out of large cores when being moved from place to place, and cores may break under the head of metal in the mold.

It is easy to appreciate the value of being able to bind sand securely with a minimum of material. Economically, through monetary savings, and physically, through less potential gas forming materials, it is wise to use the minimum amount of binder consistent with good cores.

The part that moisture plays in this baked strength

may be summarized as follows:

1. The effect of small amounts of water (between 0 and 0.4 per cent) in a core sand mixture is more pronounced than if greater quantities are used. Large quantities of water (above 3 per cent) apparently do not do more than slow down the baking. Perhaps, small quantities of water change the spacings between the sand grains and therefore the tensile strengths.

2. When core oil is the sole binder, moistureless core sand mixtures are stronger than if any amount of water is used. This is probably caused by closer grain to grain contact plus the presence of a con-

tinuous non-emulsified binding oil film.

3. Cereal binders, when used as the only source of dry bond in a core sand mixture, tend to overbake appreciably when subjected to 425 F. Experience has shown appreciable development in tensile strength when 350 F is used as the baking temperature.

4. The effect of moisture on cereal bonded cores is greater than its reaction on oil bonded ones. This may be attributed not only to the solubility of the cereal in water, but also to the greater grain to grain contact which occurs as the cereal loses its stiffness and flowability increases.

5. A combination of cereal and core oil produces significantly stronger cores than if each one were used separately. The proneness of the cereal to overbake at 425 F is apparently retarded to such an extent that it imparts a significant amount of strength to the core.

6. To get the most out of a combination of cereal and core oil as a sand binder, it is best to use them at moisture concentrations in excess of 3 per cent when lake sand is the base material. This data is graphi-

cally depicted in Fig. 1 where the average tensile strength has been plotted against per cent moisture. We believe, that since a "Tensile Strength-Baking Time curve" would peak with longer baking time with more moisture to the right of the 3 per cent moisture point on the abscissa, the negative slope of the depicted curve does not represent degradation of tensile strength, but, rather, insufficient baking time.

7. The addition of cereal to a moistureless core sand reduces the tensile strength and permeability without any apparently beneficial effects. One per cent or more of water must be used in a 1 per cent cereal-1 per cent oil combination in lake sand in order to bring out the true qualities of the dry binder.

8. When more than 2 per cent moisture is used in a core sand mixture bonded with both cereal and core oil, the core oil provides from 50 to 60 per cent of the bond.

Although the converse of this statement (i.e. cereal provides from 40 to 50 per cent of the bond) does not appear to be true if the individual data for cereal and core oil be added separately, this apparent inconsistency is thought to be caused by the retarding of the cure of the cereal binder by the core oil at 425 F.

Obviously, the cereal binder is overbaked at 425 F. The potential bond in the cereal is much greater. Unpublished data from our files shows the cereal to afford appreciable tensile strength to cores baked at 350 F for 1 hr.

When core oil is added to the cereal mixture, this effects a slower (and better) cure for the cereal therefore providing it with values in excess of the sum of the individual results.

Another phenomenon which still further complicates interpretation is the added flowability imparted to the cereal by the oil. Flowability also tends to give better tensile values due to closer grain contact.

Table 5, based on the arithmetic average tensile strength over the complete baking range, will further elucidate the effect of moisture on the tensile strength of a core sand mixture:

TABLE 5-AVERAGE TENSILE STRENGTH, PSI

| | | 42. | 10 | | |
|-------|-----------|--------|-----|----------------------------|----------------------------|
| Group | Moisture, | Cereal | Oil | Cereal+Oil (calculated) | Cereal+ Oil (actual) |
| 1 | 0.0 | 0 | 207 | 207 | 173 |
| 2 | 0.4 | 0 | 158 | 158 | 162 |
| | 0.9 | 0 | 149 | 149 | 185 |
| | 1.7 | 36 | 142 | 176 | 250 |
| | 3.4 | 99 | 146 | 245 | 282 |
| 3 | 5.0 | 133 | 144 | 277 | 273 |
| | 6.5 | 123 | 139 | 262 | 262 |
| 4 | 8.0 | 120 | 131 | 251 | 244 |
| | 9.5 | 131 | 136 | 267 | 234 |

Table 5 may conveniently be divided into four groups: Group 1, no moisture; Group 2, low to medium moisture (0.4–3.0 per cent incl.); Group 3, high moisture (5.0–6.5 per cent); Group 4, excessive moisture (8.0–9.5 per cent).

Lowering of tensile strength from the calculated in Group 1 can be explained by the dilution and absorption effect of the cereal on the core oil.

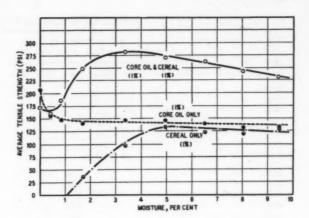


Fig. 1—Curves show relationship between core tensile strength and moisture content.

In Group 2, the actual results are all higher than the calculated ones since the full presence of the water soluble cereal binder is felt and the baking speed has been slowed down by the presence of oil.

Group 3 shows remarkable checking between the actual and the calculated results.

In Group 4, the calculated results are higher than the actual ones since the baking period has not been long enough to bring out the full value of the binders in the mixture. They have all been "swamped" by the excessive moisture which must be driven off before the baking can begin.

The most economical core sand mixtures, from a tensile strength standpoint using 1 per cent cereal and 1 per cent core oil in a lake sand, should be made at moisture between 1.5 and 4.0 per cent so that the greatest value can be secured from the binders in a minimum of baking time.

Bakability—Cube cores 4 in. x 4 in. x 4 in. in size were baked concurrently in the core oven with the tensile briquettes.

When cereal is included in a mixture, there is a definite demarkation between the soft (unbaked) and the hard (baked) portion of the interior of the core.

It might be better to term this property "apparent" bakability since we are not certain that the core oil has thoroughly oxidized once the sand has set.

Our results indicate that a core bound with cereal as the sole binder (with adequate moisture [3 to 4 per cent] to make the cereal a good adhesive) will thoroughly cure merely through moisture removal.

The addition of cereal to an oil-bonded core speeds up the apparent bakability as Table 6 shows.

TABLE 6-APPARENT BAKABILITY, PER CENT (45 min at 425 F)

| Moisture, % | Cereal | Binder Oil | Cereal+Oi |
|-------------|--------|---------------|-----------|
| 1.7 | 100 | 62 | 100 |
| 3.4 | 100 | 52 | 98 |
| 5.0 | 88 | 27 | 91 |
| 6.5 | 81 | 0 | 85 |
| 8.0 | 74 | 0 | 76 |
| 9.5 | 66 | 0 | 68 |

EFFECT OF WATER ON FOUNDRY CORE SAND MIXTURES

| Test No. Lake | _ | redients Cereal | | | hout C | | Wi | th Con | | T | ensile S | | | Bakab | (425 F) ility, % lin | Har | dness | Baker Perm No. |
|---------------------|--------|--------------------|-------|---------|--------|-------|------|--------|-----|-------|----------|--------|--------|-------|----------------------------|-------|-------|----------------------|
| Sand | | Binder | | | | (psi) | Gr. | No. | | 45 | 75 | | 135 | 45 | 75 | 45 | 75 | 140. |
| 2080 | 1.7 | | | 1.56 | 235 | 0.4 | | | | | - | | | | | | | |
| 2080 | 3.4 | | | 1.61 | 226 | 0.5 | | | | | | | | | | | | |
| 2080 | 5.0 | | | 1.62 | 205 | 0.6 | | | | | | | | | | | | |
| 2080 | 6.6 | | | 1.64 | 178 | 0.6 | | | | | | | | | | | | |
| 2080 | 8.1 | | | 1.66 | 142 | 0.7 | | | | | | | | | | | | |
| 2080 | 9.6 | | | 1.69 | 115 | 0.8 | | | | | | | | | | | | |
| 2080 | 11.0 | | | 1.72 | 80 | 0.8 | | | | | | | | | | | | |
| 2100 | 0.0 | | 1.0 | | | | 1.66 | 156 | 0.1 | 173 | 234 | 229 | 191 | | 100 | | 65-75 | 193 |
| 2103 | 0.4 | | 90 | | | | 1.62 | 178 | 0.1 | 121 | 184 | 173 | 154 | | 93 | | 65-75 | 193 |
| 2101 | 0.9 | | 99 | | | | 1.58 | 205 | 0.3 | 105 | 169 | 162 | 159 | | 88 | | 65-75 | 212 |
| 2096 | 1.7 | | 90 | | | | 1.58 | 178 | 0.3 | 96 | 163 | 158 | 150 | | 91 | | 60-70 | 205 |
| 2097 | 3.4 | | 99 | | | | 1.65 | 163 | 0.3 | 103 | 163 | 166 | 150 | | 80 | | 60-70 | 205 |
| 2098 | 5.0 | | 9.0 | | | | 1.66 | 128 | 0.3 | 94 | 156 | 162 | 162 | | 72 | | 60-70 | 205 |
| 2099 | 6.5 | | 99 | | | | 1.71 | 108 | 0.4 | 75 | 158 | 162 | 159 | | 75 | | 60-70 | 205 |
| 2102 | 8.9 | | 99 | | | | 1.75 | 80 | 0.4 | 55 | 143 | 167 | 160 | | 59 | | 65-75 | 185 |
| 2104 | 9.5 | | 99 | | | | 1.76 | 61 | 0.5 | 68 | 154 | 156 | 164 | | 51 | | 60-70 | 173 |
| 2136 | 0.9 | 1.0 | | 1.56 | 240 | 1.0 | | | | 1 | | | | | | | | |
| 2137 | 1.7 | * | | 1.61 | 200 | 0.8 | | | | 47 | 37 | 32 | 26 | 100 | | 20-30 | | 200 |
| 2138 | 3.4 | B.e | | 1.63 | 165 | 0.7 | | | | 116 | 106 | 93 | 79 | 100 | | 50-60 | | 165 |
| 2143 | 5.0 | 20 | | 1.67 | 155 | 0.6 | | | | 167 | 134 | 124 | 107 | 88 | | 55-65 | | 190 |
| 2144 | 6.5 | 88 | | 1.69 | 128 | 0.6 | | | | 157 | 128 | 109 | 98 | 81 | | 60-70 | | 180 |
| 2145 | 3.0 | 99 | | 1.73 | 105 | 0.6 | | | | 151 | 131 | 110 | 87 | 74 | | 70-80 | | 205 |
| 157 | 9.5 | 99 | | 1.74 | 75 | 0.6 | | | | 167 | 136 | 117 | 105 | 66 | | 75-85 | | 195 |
| 176 | 0.0 | 1.0 | 1.0 | | | | 1.55 | 145 | 0.2 | 104 | 179 | 210 | 199 | | | | | |
| 179 | 0.4 | 90 | 80 | | | | 1.60 | 175 | 0.3 | 122 | 187 | 174 | 166 | 8 | | | | |
| 180 | 0.9 | 99 | 99 | | | | 1.60 | 185 | 0.7 | 144 | 229 | 200 | 185 | 100 | | 80-90 | | 208 |
| 181 | 1.7 | 9.0 | 99 | | | | 1.62 | 168 | 0.7 | 133 | 303 | 294 | 271 | 100 | | 80-90 | | 185 |
| 182 | 3.4 | 94 | 99 | | | | 1.65 | 152 | 0.6 | 207 | 321 | 308 | 292 | 98 | | 90-90 | | 155 |
| 183 | 5.0 | De. | 99 | | | | 1.68 | 125 | 0.6 | 181 | 322 | 313 | 276 | 91 | | 80-90 | | 130 |
| 184 | 6.5 | 9.9 | 99 | | | | 1.72 | 102 | 0.5 | 156 | 302 | 307 | 281 | 85 | | 80-90 | | 105 |
| 188 | 8.0 | 9. | 99 | | | | 1.75 | 75 | 0.5 | 135 | 282 | 286 | 272 | 76 | | 85-95 | | 80 |
| 189 | 9.5 | 99 | 0.9 | | | | 1.79 | 47 | 0.5 | 119 | 240 | 295 | 282 | 68 | | 80-90 | | 51 |
| 1 | Tensil | e Stren | oth i | insigni | ficant | | | | | 2 Imp | ossible | to for | n core | 5 | | | | |

Again cereal and oil seem to more than complement each other for the cereal plus the oil apparently bakes out somewhat faster than the cereal itself.

Hardness-The determination of core hardness is not difficult but the results are dependent upon many variables besides the human factor. For this reason, all results are given over a fairly wide range of scratch hardness numbers. Large continuous surfaces (such as block cores) must be tested.

Our results show that:

1. Moisture does not affect the hardness of a core sand mixture appreciably where the sole bonding agent is core oil.

2. Hardness of a cereal-bonded mixture is almost a direct function of the amount of moisture in (or on) the core.

3. When more than 1 per cent moisture is present in a core sand mixture, the hardness of cores bound with cereal and oil is greater than if either one were used separately.

Summary

In any long series of tests of this nature, the reproducibility of results should be questioned. We feel that we have excellent control in our research laboratory and can guarantee results within 10 per cent for any tests cited.

We have attempted to show the general trend in room temperature testing of core sand mixtures under various core moisture conditions. Although these data cannot be directly transferred into the elevated temperature condition in the mold when the metal is founded, we believe that they may serve as a foundation from which future work can be started.

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DISCUSSION

Chairman: J. A. RASSENFOSS, American Steel Foundries, East

Co-Chairman: P. E. KYLE, Cornell University, Ithaca, N. Y. H. G. LOLLEY: 1 The author mentioned in his summary that

to gain the maximum strength from the cereal and the oil you should have at least 3 per cent moisture in the sand mix. How serious is the loss, or do you get a loss in strength if you keep

adding additional water?

MR. MYERS: There is a table in the paper showing the actual data concerning the increase in tensile strength and decrease in green bond resulting from the addition of more water than 3 per cent to the mixture. In the last part of the table starting with Test 2182 the data show what happens with increasing amounts of water (over 3.4 per cent) in a core oil-cereal binder core sand mixture. Notice that the baked strength apparently decreases. I say "apparently" because only four baking periods have been recorded. The bakability also decreases from 98 per cent to 68 per cent. The tensile strength goes from 322 odd pounds down to 295 pounds and the green strength is lowered from approximately 0.6 to 0.5 psi.

¹ Bucyrus-Erie Co., Erie, Pa.

It may or may not be significant, but we also believe that stickiness will increase as you add an excessive amount of water over 3 per cent.

G. E. DALBEY: 3 Is there any material difference in the migration to the core surface of dextrine, corn flour, and say wheat flour in the baking out as indicated by the hardness of core sur-

MR. MYERS: This migration subject has come up time and time again in the discussion of core sands. All foundrymen desire cores with hard exterior surfaces and weak collapsible interiors.

We have tried to check the migration of cereals in core sand mixtures; however, our experience has not been too conclusive. We know we get an increase in surface hardness when we add more water; also that when we try to make the hardness in the center of a core it is apparently softer. We do not know whether these high hardness readings are caused by the rammed, aligned surface sand grains forming a plane surface against which to scratch or whether it is due to the migration of the binder.

Migration can be checked somewhat through the use of an iodine solution. Cut a cross-section of the core and dip it in the iodine solution. A thin purple line reveals starch concentration. We have not found appreciable difference between corn, wheat, and dextrine although (historically) dextrine is supposed to migrate more readily.

MEMBER: With your oil-sand core mixture you get a dense core. What is your chance of getting excessive core blows and

chill spots on thin section castings?

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MR. MYERS: Dense cores, made for radiator castings, did not produce chill spots in the metal. The amount of chilling depends upon the metal, type of sand, and other things of that nature. I do not think you would expect too much trouble from a "straight" oil-sand mixture although the permeability is de-

W. R. MOGGRIDGE: ^a In the paper the author made this statement on relative humidity: "Remarkably consistent results were obtained despite the use of different batches of sand and different meteorological conditions each day. Records were kept of the relative humidity and room temperature during the entire series of tests. No correlation could be detected between these data and the test results." How do you explain the absence of any correlation between the tests and relative humidity? Is it because the oven atmosphere was controlled, or is it because of the grain size of the sand?

MR. MYERS: It is neither. We wanted to be on record to say that we have no data which would substantiate any difference that might be expected from the difference in relative humidity. We find you can get more reproducible results in test briquettes if you put them in a desiccator after baking. If they are left out in the laboratory for any length of time before they are broken we feel that relative humidity will influence reproducibility. But if you put them in a desiccator, I do not believe you will have any difficulty in that direction. All we wanted to show you was that we could find no reason why we should not get reproducible results.

MR. MOGGRIDGE: I was wondering if the relative humidity had any effect during the baking period. If you had high relative humidity on one day and low relative humidity the next day would the baking speed in the open be affected?

MR. Myers: We believe we can take standard sand, raw linseed oil and distilled water and reproduce results within 10 per cent despite any change in relative humidity and temperature during the time the test is made. We believe we could run a test and reproduce a result within 10 per cent no matter what the humidity is. A lot of moisture is produced through the combustion of the gas and perhaps that overshadows any difficulty you might have because of meteorological conditions.

CHAIRMAN RASSENFOSS: What precautions do you take in testing technique to be certain you will be able at a later date to

reproduce the results within 10 per cent?

Mr. Myers: It was stated in the paper that at least ten briquettes are tested for every point on the curve, and by "at least," I mean that is the minimum. In the first place, you have to have standard sand. We believe the pH of the mixture is necessarily controlled. Therefore, you must have distilled water.

We would like to use standard linseed oil. You have to control your mixing time, the method in which your mixer is cleaned, the speed at which you make your specimens, the way the specimens are put on the plate, the way the plates are put into the oven, when they are removed from the oven, their position in the oven. You have to check that they stand out in the room the same time in every case, in other words until they are cool to touch. They must be matched so they are put in the tensile testing machine with the same side out in every case. The results must be recorded with some mathematics behind it and not "throwing away any obvious or erroneous ones." After that you must run the whole test again to be certain.

CHAIRMAN RASSENFOSS: The reason I asked that question was to impress the reader with the fact that sometimes materials are so similar in performance that to determine the true difference you have to be careful in making tests. It also brings forward to the foundryman the idea you cannot take two or three materials and carelessly mix them together and expect to get satisfactory results from week to week in making actual castings.

MEMBER: Do you find that the size of the batch is an important factor? You used 25-lb batches. I have been using You used 25-lb batches. I have been using 2,000-gram batches and I have had trouble in reproducing my results.

MR. MYERS: We do not have a small mixer. We did all our work in a 25-lb batch mixer. If we got a tensile strength of 175 pounds from a 25-lb batch, we probably could not reproduce those results using a 2,000-lb batch. I do not believe you would get exactly the same results between the 18-in. and 24-in. mixers.

CHAIRMAN RASSENFOSS: We have an 18-in. muller and that is almost the minimum size batch we would put in. We would normally like to run more than that because if the ratio of the surface area to the volume is too great difficulty is experienced controlling the moisture content. With mixtures of this type that are low in green strength, we can get up to 7,000 grams per batch. It is possible to get poor results with large batch sizes because the sand gets so deep in the muller that the plows do not go through their proper action and then you are in trouble again. Perhaps there is an optimum of 5,000 grams but I should think that keeping the surface area at a minimum with respect to the volume would tend to give more uniform results. The smaller the batch size the more careful you have to be.

MR. MYERS: We concur with that and feel that green strength is a determining factor in deciding the size of the batch in the mixer.

E. E. WOODLIFF: 4 This bakability test using a 4-in. cube is quite significant. It gives the foundryman something to think about in terms of retained water in the center of the cores. Quite frequently moisture may be a source of core trouble.

This cube core test is still not a very accurate core test because the core still has to be rammed by hand. I have no means of ramming the core under a standard rammer. This cube core when baked with a varying amount of water, or whatever your variable might be, a certain percentage of the core is still unbaked. That core is drilled through with a drill from side to side and a moisture content is run on the cross-section of the sand several days later. It is also determined on a like core after storage, and moisture does not change, but the moisture has migrated throughout the baked portion of the core, which means that a core in storage has not picked up moisture from the air but has simply distributed the water throughout. Water is the source of difficulty in a core which scabs in the foundry.

I do not agree that we should always work our moisture at 3 per cent or above. Keeping the moisture low has some advantages too.

H. K. SALZBERG: 5 Further, on the statement that at least 3 per cent moisture should be used in a combination of sand, oil, cereal and water, that applies, I presume, to this particular concentration of cereal in the sand. If the cereal were higher you would name a higher minimum figure for the water content to bring out the maximum dry properties. Is that correct?

MR. Myers: That is correct. The cereal ratio should be made 3 to 1 at least, three parts of moisture to one of cereal, in order to bring out the maximum properties of the core.

² Mare Island Naval Shipyard, Vallejo, Calif.

⁸ Ford Motor Co. Ltd., Windsor, Ont., Canada.

^{*} Foundry Sand Service Engineering Co. Detroit.

⁵ The Borden Co., Bainbridge, N. Y.

FAYALITE REACTION IN SAND MOLDS USED FOR MAKING STEEL CASTINGS

By

Robert E. Savage** and Howard F. Taylor***

ABSTRACT

When molten steel is poured into a sand mold the mold-metal interface undergoes a chemical and physical change. The exact nature of the change and the composition of product formed have never been determined uniquely. Some of the defects which may be caused by interface phenomena are mechanical penetrations, cope defects, scabs, spalls, and surface roughness. Knowledge of what happens at the interface of the mold may eventually lead to solutions of these problems.

Surface finish is second only to satisfactory service performance as a requirement of a good steel casting. In general surface finish is a function of—

- 1. Pouring temperature and fluidity of the steel,
- 2. Grain size and distribution of the molding sand,
- 3. Mold hardness, or degree of packing of the sand,
 4. Type of mold, i.e., whether "green" or dried, and
- 5. Results of interface reactions between the metal and the mold, if such reactions occur.

The first four variables are reasonably amenable to control; this report concerns only the last variable, mold-metal interface

Fayalite, 2FeO.SiO₃, was found by x-ray diffraction analysis to be the only product formed by the interface reaction. No other elements were found in the interface layer. This was true of shop and laboratory samples. The fayalite was formed by oxidation of the iron of the steel and by subsequent reactions of this oxide with the silica sand (SiO₃).

Introduction

Caine,^{1,2} Dietert,^{3,4,5,6} and others,^{7,8} have made superficial tests to determine the general characteristics of sand-steel interface reactions, and have made cut-and-try changes in sand mixtures in an attempt to bring about some understanding of the process. No quantitative approach to the problem is to be found in the literature. Taylor and Morey,⁹ in a discussion of one of Dietert's papers, describe a laboratory test developed by them which appeared to simulate actual production conditions. In this test, a small steel pin, buried in sand and heated nearly to melting, gave a definite interface product which resembled closely the "sinter" obtained in actual casting. Again, no quanti-

tative tests were made to analyze and verify the findings. Atmospheres were varied roughly from oxidizing to reducing, and plain carbon and stainless steel pins were used. In reducing atmospheres and with stainless steels, no interface product formed. From the superficial tests it appeared, according to Taylor and Morey,9 that the reaction product was the result of oxidation of the steel and subsequent reactions between the iron oxide so formed and the silica sand to form some low melting glaze. The experiments, although inconclusive, indicated that much of value might be learned about the behavior of molding sands by improving the test method and by studying the problem quantitatively. The chief limitations of the test were that (1) the pin was heated through the sand (heating was done in an ordinary muffle furnace) and so did not represent actual conditions, and (2) the steel pin was not melted.

Experimental Procedure

The program of study was-

1. To first obtain samples of reaction product—socalled "peel" or "sinter-layer" from a foundry making fairly large steel castings. Samples were obtained from the Hunt-Spiller Manufacturing Corp., Dorchester, Mass.; Figure 1 is a typical specimen.

2. It was suspected that fayalite was at least one of



Fig. 1—Typical "sinter layer" removed from a steel casting.

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Part of a thesis for Doctor of Science Degree at Massachusetts Institute of Technology, Cambridge, Mass.

^{*} Metallurgist, The International Nickel Company, Inc., New York.

^{***} Associate Professor, Metallurgy Department, Massachusetts Institute of Technology, Cambridge, Mass.

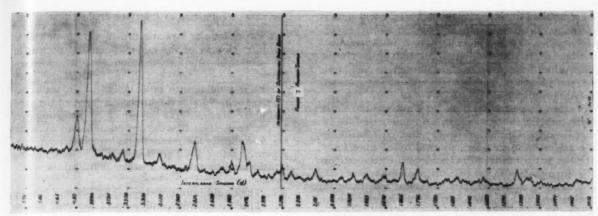


Fig. 2

the reaction products. The composition of fayalite is well known (2 FeO.SiO₂). The material cannot be isolated chemically so an analysis by x-ray diffraction was required.

3. In order to study the effect of atmospheres, mold material, metal, and other variables upon interface phenomena, a small scale laboratory test was needed; remembering the applications and limitations of Taylor's and Morey's pin test, it was decided that a steel pin embedded in a sand specimen might reproduce shop conditions if the pin could be melted *in situ* by induction. The reaction product of the laboratory test specimen could be compared by x-ray analysis with the shop samples.

Experimental Results

To spare the casual readers relatively unimportant detail an appendix is added; this includes—

- A description of the x-ray unit and its operation,
 Composition of the molding sand and the metal
- for the Hunt-Spiller samples of interface product,
 3. Details for preparation of the synthetic fayalite
 used as a comparison standard,
- X-ray diffraction patterns of silica sand and bentonite under different atmospheres and temperatures, and
- Theoretical considerations of mold-metal interface reactions.

Identification of Fayalite

The x-ray spectrometer, Appendix 1, can be used to identify chemical substances and their state of chemical combination. Most compounds give off a characteristic pattern when an x-ray beam is diffracted by the interplanar spacings in the compound. Each pattern is distinct and identifiable from every other compound, and an unknown sample is identified by comparison with a previously recorded compound.

Unfortunately all compounds are not yet reported in the ASTM X-ray Card Index System, and some which are reported are in error. The lines for fayalite, needed for this investigation were found to be in error. Correspondence on the British and German fayalite, included in the Index, suggested doubtful purity of the source material. Accordingly it was necessary to produce synthetic fayalite and to establish

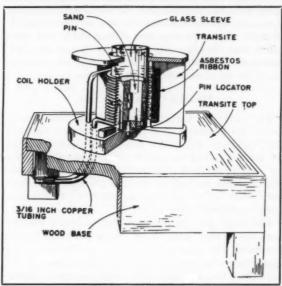


Fig. 3—Induction furnace with specimen in position for testing.



Fig. 4—Photo of laboratory induction furnace showing typical specimen

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correct values for interplanar spacing (d) and relative intensity (I) before proceeding further with the interface studies. These values are given in Appendix 3 with the descriptions of the preparation of the sample. The values have since been submitted to ASTM for the use of others interested in conducting interface or other studies.

The x-ray spectrograph of the Hunt-Spiller "sinter-layer," Fig. 2, shows quartz, tridymite, cristobalite, and fayalite. Quartz, tridymite, and cristobalite are the original and transformation forms of silica found at room temperature, 870 C, and 1470 C, respectively. No attempt was made to isolate completely reacted grains; many appeared fully blackened but fracturing showed them to be only partially or superficially reacted. The extra lines of quartz and its transformation forms did not interfere with the fayalite lines, so the separation was not essential. No iron, or other metallic elements, nor any compound of iron oxide and silica other than fayalite was found by the x-ray spectrometer. This means that if present, such elements would be an unimportant trace at most.

Modified "Sinter-pin" Test

The induction furnace, Fig. 3 and 4, proved satisfactory for heating a steel pin buried in a cylindrical sand specimen, Fig. 5a, b and c. Thermocouples on the surface and at the center of the pins, and optical readings through slits in the sand specimen showed that complete melting occurred in 2.8 min. Most tests were made at 3.5 min heating time to provide adequate superheat in the metal.

The sinter pin test gave interface products visually identical to the "sinter layer" from the Hunt-Spiller specimen. The spectrograph, Fig. 6, verified the visual appearance—the same reaction had taken place in the laboratory test as resulted in shop practice. Laboratory testing was thus found a reliable index and was faster, cheaper, more controllable, more versatile, and simpler than shop testing. Only a few of the studies using this technique will be discussed here; work is continuing in an attempt to isolate the many variables effecting surface finish of castings.

Fig. 6

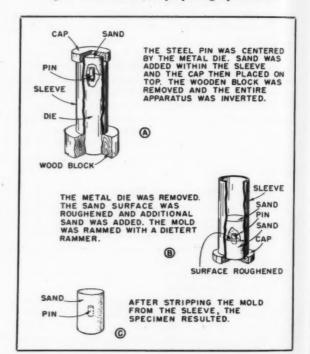
Formation of Interface Product

A theory for the formation of fayalite found by x-ray diffraction to be the major compound present in the sand-steel interface reaction product, is given below:

Steel is poured into sand molds in an air atmosphere; the mold and the interstices between the sand grains are filled with air, of which oxygen comprises 21 per cent. The iron-oxygen equilibrium diagram, Fig. 7, indicates the reactions taking place when iron is poured into a silica mold at 1650 C. The following transformations take place under equilibrium conditions:

The liquid iron is oxidized to 1/4 per cent oxygen.
 This liquid iron containing 1/4 per cent oxygen is in equilibrium with liquid oxide of 221/4 per cent

Fig. 5-Procedure for preparing specimens.



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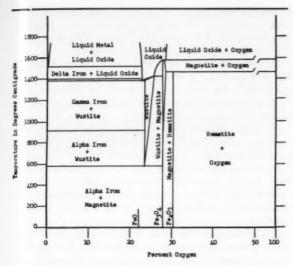


Fig. 7-Iron-Oxygen Equilibrium Diagram.

oxygen. (This corresponds closely to the stoichiometric ratio of oxygen in FeO.) An interface layer will move progressively from the surface of the metal inward until all the metal has the composition of 221/4 per cent oxygen and is essentially an oxide.

(a) Liquid oxide can increase in oxygen content from 22½ per cent to 27½ per cent, and the oxide phase formed is homogeneous throughout this region. Liquid oxide of 27½ per cent oxygen is in equilibrium with oxygen in the atmosphere and will not oxidize further.

(b) The FeO-SiO₂ equilibrium diagram, Fig. 8, helps to illustrate the theory of fayalite formation. The liquid oxide, item 2 above, is of a composition close to FeO; it is very fluid and readily flows between and around the sand grains at the interface, actually dissolving them partially or completely. The sand will continue to be dissolved until the melt is 50 per cent SiO₂. If the composition of the melt lies to the right of the fayalite composition (70.5 per cent FeO), fayalite will solidify and precipitate the excess FeO upon cooling; if the melt composition lies to the left of this value, fayalite will solidify and precipitate the excess SiO₂. Experimental evidence shows that fayalite with excess SiO₂ forms at the mold-metal interface due to the excess of silica sand.

A. Effect of Elements Dissolved in Steel on Interface Reactions—Elements dissolved in steel will alter interface reaction somewhat from that which forms fayalite when pure iron is poured into a sand mold. The more easily oxidizable elements will decrease the amount of oxygen available for oxidation of the iron. Silicon, carbon and manganese are depleted from the surface layer of the casting. The depletion of these elements in the surface layer involves a process which is dependent upon time and temperature. With an increase in the carbon content of the steel, the fayalite reaction is retarded since carbon in the surface layer of the cast-

ing is oxidized before the iron. Silicon and manganese have a lesser effect than carbon in retarding fayalite formation.

B. Effect of Sand Additions on Interface Reactions-Bentonite tends to promote the formation of favalite. Bentonite consists of a complex silica-alumina compound with small amounts of other oxides present. This research has shown its melting point to be below 1400 C; at steel pouring temperatures, bentonite is fluid and provides the medium for solution of the liquid iron oxide. The melt approximating the fayalite composition has a solubility for Al₂O₃, so the bentonite could be dissolved in the melt formed. The reaction between FeO and SiO2 is initiated around the sand grains on the bentonite layer. The sand grains are coated with liquid bentonite, which encourages the iron oxide to flow around the individual grains. Bentonite promotes the favalite reaction by dissolving the iron oxide quickly and making it more fluid than it would be otherwise. The melt thus formed will dissolve more and more silica until the reaction is essentially one between FeO and SiO2, as described in item 2 (b) above. The melt formed precipitates fayalite on

Organic binders which burn out at low temperatures will have little effect on the basic reaction between sand, steel, and oxygen, except that organic binders which produce a reducing gas tend to retard the fayalite formation. Such reducing gases would inhibit the oxidation of the iron at the casting surface.

C. Skin Formation at Casting Surface—If it is assumed that a solid skin forms immediately after the metal contacts the cold sand, fayalite formation can be explained as follows:*

This is based on the knowledge that an interface product can be formed in ample amount at temperatures well below the melting point of steel pins. In fact, an entire pin buried in sand can be oxidized relatively quickly without ever reaching its melting temperature.

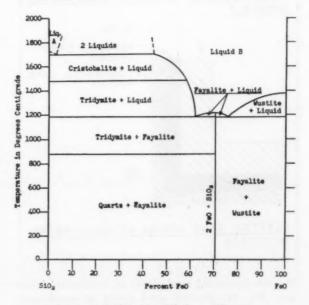


Fig. 8-FeO-SiO₂ Equilibrium Diagram.

^{*} In general, plain carbon steels differ in composition from pure iron only by the presence of 0.2 to 0.4 per cent carbon, 0.3 to 1.0 per cent silicon, 0.6 to 1.0 per cent manganese, and traces of other elements.

1. Assume the steel poured at 1650 C, surface of the metal adjacent to the sand is chilled and a thin skin is solidified immediately.

2. The iron of the solidified shell at the interface will form a liquid oxide approximating FeO. This reaction will take place from the solidification temperature of the iron at 1380 C.

3. This liquid FeO may:

(a) Oxidize further to Fe₃O₄ (this would occur only if oxygen can reach the surface faster than it can diffuse through the FeO into the metallic iron).

(b) React with the excess SiO₂ present, as described in item 2 (b) above. This reaction will occur down to 1210 C. When the melt solidifies to form fayalite.

The behavior of the mold-metal interface soon after the steel is poured into the mold is a good example of how the interface product forms. The sketches "a" and "b" of Fig. 9 represent actual castings; the metal was "bled" by inverting the mold "a"—30 sec and "b"—3.5 min after pouring. In "a" the sand grains are not discolored and are locked so tightly to the casting that sand blasting does not remove them—in 30 sec some penetration has occurred but no interface reaction product has formed. In 3.5 min the sand has turned black to a considerable depth. The "sinter layer" shears automatically from the casting, due to its dif-

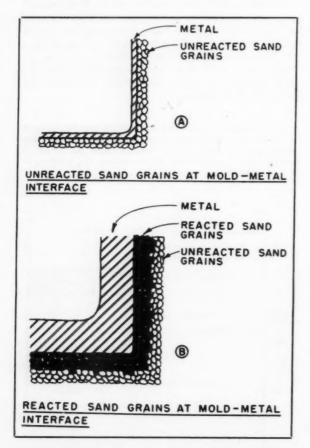


Fig. 9A—Unreacted sand grains at mold-metal interface; Fig. 9B—Reacted sand grains at mold-metal interface.

ferent thermal contraction, and leaves a sand free, smooth surface on the casting if the sand size and grain distribution has been properly chosen. This shows that the interface reaction is time dependent.*

D. Effect of Iron Vapor Pressure in Mold on Mold-Metal Reactions—It has been suggested that iron is transferred through a sand mold by vaporization. Such a mechanism is held to explain the ready penetration of steel into sand molds.

No condensed iron was visible on the copes of the two-piece molds used in sinter-pin tests. The cope sand did not have its characteristic black color, since the iron was not in contact with the sand when heated. Iron has an extremely low vapor pressure in the molten state, and penetration of steel into the sand at the interface is obviously not at all due to this phenomenon.

Calculations can be made from data obtained from the laboratory pin test to show more clearly that iron vapor pressure is not an important consideration in loss of iron to the sand mold. The total volume of a sand specimen $2\frac{1}{2}$ in. high and 2 in. in diam is 7.85 cu in. The volume of the steel pin $\frac{3}{4}$ in. high and $\frac{1}{2}$ in. in diam is 0.15 cu in. Therefore, the volume occupied by the sand is 7.70 cu in., or 126 cc. Assuming that the entire sand mass was filled with iron vapor at the specified temperature, Table 1 is developed.

TABLE 1—IMPORTANCE OF IRON VAPOR PRESSURE ON METAL TRANSFER TO THE MOLD

(Entire mold assumed to be at specified temperature)

| Temp., | Fe Vapor Pressure, mm Hg | Fe Wt.—gm in 126 cc | Pin Average Fe Wt. Loss in gm | Per Cent Loss due to Vaporization |
|--------|--------------------------------|-------------------------|-------------------------------------|---|
| 1527 | 0.023 | 9.5 x 10 ⁻⁴ | 0.185 | 0.00514 |
| 1727 | 0.220 | 90.9 x 10 ⁻⁶ | 0.185 | 0.0491 |

Two factors tend to make the loss due to vaporization less than shown: (1) the temperature drops rapidly in the sand mold from the surface of the casting outward, and the vapor pressure would also drop rapidly in this region; and (2) the vapor pressure of the iron is present only in the void spaces of the sand mold, and such void spaces are only a small percentage of the total mold volume. With these modifications, the above table shows that the role of vapor pressure is a minor one in any loss of iron to sand molds.

E. Effect of Gas Atmospheres on Interface Reactions—Reactions occurring in air may be partly or wholly altered by changing the gas atmosphere surrounding the reactants. It is realized that gas atmosphere, per se, would not be a practical industrial expedient. However, if atmosphere control proved outstandingly good, gas producing additions might be incorporated in the molding material.

A globar tube furnace and pure materials were used to study the effect of gas atmospheres on mold-metal reactions. The gases used were hydrogen, nitrogen, and air, to provide reducing, inert, and oxidizing conditions, respectively. X-ray diffraction charts were

^{*} Sand often sticks worse to small, thin castings than to larger ones; this may be due to lack of enough time and/or temperature.

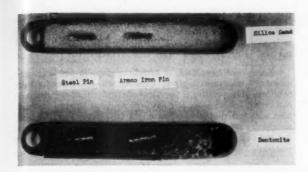


Fig. 10-Photo of typical experiment for studying effect of atmospheres on mold metal reactions.

used to identify the phases present after the reactions took place. Figure 10 shows a photograph of a typical experiment in which the reactants were heated in a gas atmosphere for 1 hr at 1400 C. Armco iron and 0.8 per cent carbon steel pins were embedded in silica sand or bentonite held in combustion boats. The boats were then heated under gas atmospheres for specified times and temperatures.

1. Reducing Atmosphere (Hydrogen)-The Armco

iron pin embedded in silica sand at 1400 C for 1 hr in a hydrogen atmosphere showed no visible change. The pin had a high metallic luster and retained its original shape except for small pockets on the bottom where the metal had flowed around the sand grains to a slight degree. The sand peeled readily from the pin. No mold-metal reaction product was formed.

The 0.8 per cent carbon steel pin embedded in sand had changed its shape due to incipient melting, but there was no mold-metal reaction. The metal had flowed around the sand grains, causing small pockets at the interface. The sand peeled readily from the pin.

The Armco iron pin embedded in bentonite retained its original shape; it retained its metallic luster and was readily removed from the fused bentonite. There was no mold-metal reaction. The 0.8 per cent carbon steel pin embedded in bentonite changed its shape to a slight degree, but the pin surface still retained a metallic luster. No mold-metal reaction product was formed.

Iron is not oxidized in a hydrogen atmosphere, and so fayalite does not form as it does in air. The surface of the pins tested had not reacted with either the bentonite or the silica sand. Casting under a reducing atmosphere would definitely be desirable if practical.

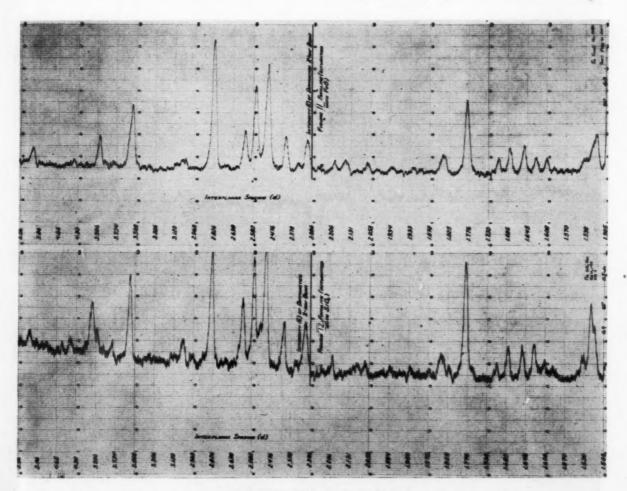


Fig. 11, Top, and Fig. 12, Bottom.

Hydrogen atmospheres cannot be used commercially to solve the mold-metal reaction problem, as hydrogen is expensive and also dissolves in steel, creating even worse difficulties than poor surface finish. A more practical approach would be the use of carbon mixed in the sand or used as a mold wash; this might provide a proper reducing atmosphere of carbon monoxide.*

2. Inert Atmosphere (Nitrogen)—When Armco iron was heated with silica sand in a nitrogen atmosphere at 1400 C for 1 hr, no mold-metal reaction product was formed. The pins showed small pockets where

the metal had flowed around the sand grains; however, the pin retained its overall metallic luster. A steel pin with 0.8 per cent carbon embedded in silica sand melted slightly more than the Armco iron pin. The

• The use of lampblack or fine flake graphite has long been used by foundrymen without knowing why good results were obtained. The practice is prevalent particularly among iron and nonferrous foundrymen, but has been shunned by steel foundrymen who feel that the carbon might be absorbed by the steel. It appears from this investigation that their fears are groundless and that carbon could be used advantageously as an interface material.

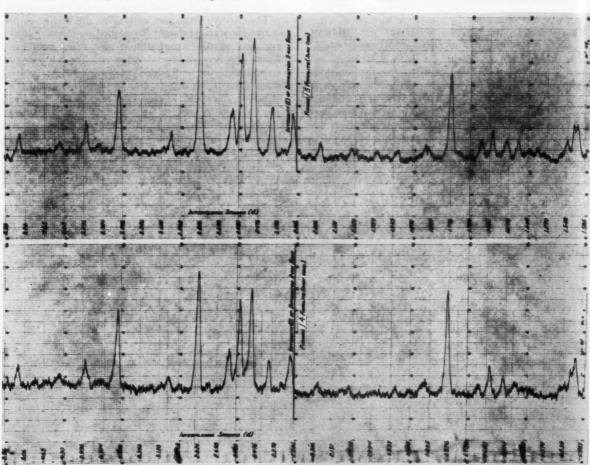
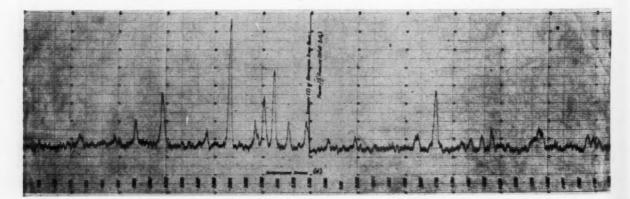


Fig. 13, Top, and Fig. 14, Bottom.

Fig. 15-Below.



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surface was rougher, but the pin still retained its metallic luster. No mold-metal reaction product was formed on the pin surface.

When oxidized pins of Armco iron and 0.8 per cent carbon steel were embedded in silica sand and heated in an atmosphere of nitrogen for 1 hr at 1400 C, a black mold-metal reaction product was formed in each case. This black interface material peeled from the pin, leaving a metallic luster. The reaction product resulted from the oxide layer which had been formed purposely before the pins were placed in the nitrogen atmosphere. Fayalite lines were present in the x-ray diffraction patterns of the reaction products.

When $\mathrm{Fe_2O_3}$ and $\mathrm{SiO_2}$ were heated for 1 hr at 1140 C in a nitrogen atmosphere, fayalite was not present in the x-ray diffraction pattern. By raising the temperature to 1250 C for 1 hr, fayalite was found. The formation temperature of fayalite was found to be in the temperature range from 1140 to 1250 C when the reactants were heated in an inert atmosphere.

If steel were poured in a nitrogen atmosphere, no fayalite reaction would occur at the mold-metal interface. Nitrogen is soluble in liquid steel to a limited extent and might cause trouble, so a nitrogen atmosphere would not be used.

3. Oxidizing Atmosphere (Air)-The greatest pro-

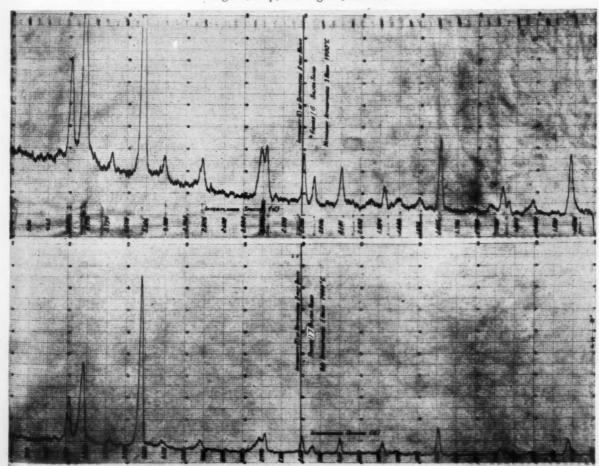
portion of steel castings are produced under oxidizing atmospheres and all laboratory tests, except those just discussed, were done in air. The results showed that fayalite would form from iron only under existing conditions.

APPENDIX 1

Description of X-ray Spectrometer

An x-ray spectrometer was used to identify the interface reaction products. The principal components are an x-ray source, collimating slits, a specimen mount, and a Geiger counter tube with slit system mounted on a 90-deg quadrant scanning arm. The specimen mount is geared so that it rotates at half the angular speed of the scanning arm and Geiger counter. For this reason, the Geiger counter is maintained at an angle to the surface of the specimen equal to the angle of incidence of the x-ray beam. Therefore, the counter is always in position to receive the diffracted beam when a given set of crystal planes in the specimen are at an angle with the incident radiation equal to the characteristic Bragg angle for those planes. Thus, a plot of the intensity of the diffracted x-ray beam versus the angle of diffraction was drawn by the instrument. All x-ray charts were recorded with an iron tube and a manganese filter.

Fig. 16, Top, and Fig. 17, Bottom.



APPENDIX 2

Sand and Metal Composition of Foundry Sample

To identify the compounds present at the interface between a typical silica sand mold and a steel casting, a representative sample was taken from a one-ton casting produced by the Hunt-Spiller Manufacturing Corp. of Dorchester, Massachusetts. The facing sand mixture used for making this particular casting was

| Ingredient | Per Cent |
|----------------------------|----------|
| Silica Sand | 96.0 |
| Western Bentonite | 3.0 |
| Cereal Binder (Corn Flour) | 1.0 |
| Water added to mixture | 3.0 |

and the composition of the steel is given below:

| Element | Per Cent |
|------------|----------|
| Carbon | 0.35 |
| Silicon | 0.40 |
| Manganese | 1.0 |
| Sulphur | 0.03 |
| Phosphorus | 0.035 |
| Molybdenum | 0.30 |
| Chromium | 0.70 |
| Nickel | 0.45 |
| Iron | Balance |

The casting was shaken from the mold approximately 3 hr after pouring. The black interface material was 1/4 in. thick and peeled freely from the casting in large sheets.

APPENDIX 3

Synthetic Fayalite Used as Comparison Standard

The synthetic fayalite was made by thoroughly mixing 320 grams SiO₂, 500 grams Fe₂O₃, and 180 grams of Fe in an Armco iron crucible; the material was then placed in a muffle furnace at 700 C and a temperature of 1270 C was reached after 2 hr; at this temperature the mixture was molten. After holding for 15 min at 1270 C, the crucible was emptied into an iron mold. Chemical analysis showed 27.89 per cent SiO₂, 67.49 per cent Fe⁺⁺, and 4.24 per cent Fe⁺⁺⁺ (Total—99.62 per cent).* Normally, fayalite does not contain Fe⁺⁺⁺. The small percentage of Fe⁺⁺⁺ present in the synthetic sample was not dissolved in the fayalite, but was present as Fe₂O₃. In any fayalite sample made in an air atmosphere some iron is further oxidized and comes out of solution when fayalite solidifies.

Fayalite was synthesized in two ways: (1) with excess FeO and (2) with excess SiO₂, to determine the effect of such combinations on the diffraction pattern. In both cases, the lines of the excess constituent were superimposed on the fayalite lines. The fayalite lines from the two charts were essentially the same. Fayalite has no solubility for FeO or SiO₂. The diffraction patterns for fayalite saturated with FeO and with SiO₂, are shown in Fig. 11 and 12, respectively.

There is no noticeable difference in the x-ray paterns of slowly or rapidly cooled fayalite, Fig. 13 and 14.

* The exact stoichiometric proportions of fayalite should be 29.5 per cent SiO₂ and 70.5 per Cent Fe⁺⁺.

The values of d* listed in Table 2, were obtained from a slowly cooled fayalite sample. The diffraction pattern of this sample is shown in Fig. 15.

TABLE 2—DIFFRACTION PATTERN OF FAYALITE (2 FEO . SIO₀)

| Interplanar Distance d in Angstrom Units | Relative Intensity 1/1 Maximum |
|---|---------------------------------|
| 5.26 | 0.09 |
| 4.37 | 0.06 |
| 3.97 | 0.18 |
| 3.56 | 0.41 |
| 3.04 | 0.13 |
| 2.82 | 1.00 |
| 2.63 | 0.21 |
| 2.56 | 0.40 |
| 2.50 | 0.62 |
| 2.40 | 0.21 |
| 2.30 | 0.21 |
| 2.19 | 0.08 |
| 2.07 | 0.10 |
| 1.84 | 0.11 |
| 1.835 | 0.12 |
| 1.78 | 0.47 |
| 1.702 | 0.06 |
| 1.675 | 0.09 |
| 1.650 | 0.11 |
| 1.625 | 0.18 |
| 1.588 | 0.05 |
| 1.524 | 0.18 |
| 1.429 | 0.11 |
| 1.42 | 0.11 |
| 1.411 | 0.07 |

The values of the interplanar distances and relative intensities for the three major lines are:

| Interplanar Distance d in Angstrom Units | Relative Intensity 1/1 Maximum |
|---|---------------------------------|
| 2.82 | 1.00 |
| 2.50 | 0.62 |
| 1.78 | 0.47 |

APPENDIX 4

X-ray Diffraction Patterns of Silica Sand and Bentonite Under Different Atmospheres and Temperatures

Quartz is stable up to 870 C; it transforms at this temperature to tridymite, which is stable to 1470 C. Cristobalite is the stable form from 1470 C to the melting point of silica at 1728 C. These reactions are dependent upon time and temperature, and are reversible to some degree. Although it was expected that these reactions would prevail similarly in atmospheres of nitrogen or air, x-ray examinations were made of samples of silica sands available, heated in these atmospheres. Diffraction patterns of silica sand heated for 1 hr at 1400 C in atmospheres of nitrogen and air are shown in Fig. 16 and 17, respectively. Comparison of the x-ray charts show identical transformations in each atmosphere. The individual sand grains heated in hydrogen for one hour at 1400 C are clearly visible in Fig. 10.

Since bentonite is the major addition to sand mixes in most steel foundries, x-ray patterns were made of

^{*} These values were contributed to the ASTM Card Index System for the compound fayalite.

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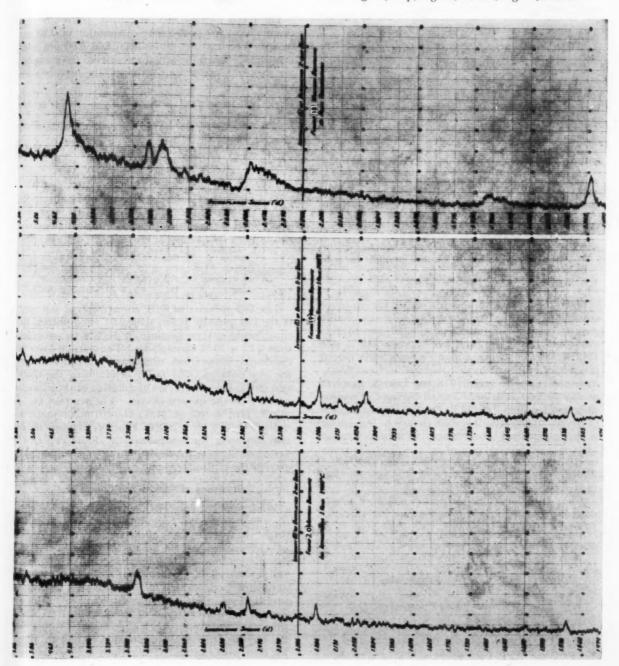
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the material under nitrogen and air atmospheres at two different temperatures. Western bentonite used in these experiments had the chemical composition:

| Compound | Per Cent |
|--------------------------------|----------|
| SiO ₂ | 60.64 |
| Al ₂ O ₃ | 23.26 |
| Fe ₂ O ₃ | 3.92 |
| TiO ₂ | 0.12 |
| CaO | 0.59 |
| MgO | 2.19 |
| K ₂ O | 0.37 |
| Na ₂ O | 4.33 |
| H ₂ O | 2.83 |
| TOTAL | 98 95 |

Bentonite underwent a specific change upon heating in various atmospheres; this change is registered by the x-ray diffraction patterns shown in Fig. 18 (unheated), 19 (1400 C in hydrogen for 1 hr), and 20 (1400 C in air for 1 hr). A shift in location of the peaks is evident; the peak located at d = 2.206 Angstrom units was due to the presence of iron vapor which condensed over the surface of the boat containing the bentonite. Figure 10 shows this boat with the steel and Armco iron pins embedded in the bentonite. Bentonite is a white powder at room temperature; it

Fig. 18, Top, Fig. 19, Center, Fig. 20, Bottom.



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had melted at this temperature (1400 C). Its color changed to dark gray, and the bentonite swelled to a honey-combed, slag-like mass. The x-ray diffraction pattern of Fig. 20 shows western bentonite heated for 1 hr at 1400 C in an air atmosphere; the powder melted and swelled to a honey-combed texture as before, but with a brown, slag-like appearance. There is marked similarity between the diffraction patterns of western bentonite heated in hydrogen and in air. Fewer peaks in the heated specimens indicate that bentonite underwent a partial transformation into a vitreous state, and that the process was not reversible upon cooling. Thus, bentonite heated to this temperature in a sand mold, even without contact with iron oxide, undergoes a structural change.

APPENDIX 5

Theoretical Considerations of Mold-Metal Interface Reactions

A. Oxidation of Iron—Iron can combine with oxygen to form one of the three iron oxides. The change in free energy for the oxide formation is given by Equations 1 through 5.

(1) 2 Fe (s) + O₃ (g) = 2 FeO (s) (Wustite)
$$\triangle F^{\circ}298-1642^{\circ}K = -124,000 + 29.90 T$$

(2) 2 Fe (s) +
$$O_2$$
 (g) = 2 FeO (1) \triangle F°1642-1808°K = $-103.950 + 17.71$ T

(3) 2 Fe (1) +
$$O_2$$
 (g) = 2 FeO (1) $\triangle F^{\circ}1808-2000^{\circ}K = -111.250 + 21.67 T$

$$-111.250 + 21.07$$
 1
(4) $\frac{8}{2}$ Fe (s) + O₂ (g) = $\frac{1}{2}$ Fe₂O₄ (s) \triangle F° 298-1642°K = $-130.400 + 37.2$ T

(5)
$$\%$$
 Fe (s) + O₂ (g) = $\%$ Fe₂O₅(s) \triangle F° 298–1460°K = $-129,000 + 40.55$ T

$$T = Absolute temperature (t °C + 273°).$$

When the activities of the iron and of the oxide are assumed to be 1, the free energy change is numerically greatest negatively for the oxidation of iron to FeO above 843°K. At temperatures between 843°K and 1642°K, the iron will oxidize to FeO at a lower partial pressure of oxygen than that required to form Fe₃O₄. At temperatures between 470°K and 1460°K, iron will oxidize to FeO at a lower partial pressure of oxygen than that required to form Fe₂O₃. For a given amount of oxygen, the greatest negative change in free energy is obtained when FeO is formed. In foundry practice there is a definite amount of oxygen available at the interface to oxidize the iron of the steel to FeO, which subsequently combines with the SiO₂ in the sand mold to give fayalite on cooling.

From Equation 6, the partial pressure of oxygen necessary to oxidize the iron to FeO at 1808°K is 1.9 x 10-9 atmosphere.

(6) 2 Fe (1) + O₂ (g) = 2 FeO (1)
$$\triangle F^{\circ}_{1808}^{\circ}_{1808} = -72.000 = -RT Ln \frac{n^{3}FeO}{n^{3}Fe.Po_{2}} = = -1.986$$
 (1808) Ln $\frac{1}{Po_{2}}$ Po₂ = -1.9 x 10⁻⁹ atmosphere

of iron is greater than that required to form FeO above 843°K.

B. Oxidation of Elements in Steel—When molten steel is poured into a sand mold, carbon, silicon and manganese (as well as other elements) are dissolved in the iron. Some of these elements combine with the oxygen in the mold preferentially to the iron. Equations 7 through 9 relate the free energy change with the temperature for each oxidation reaction.

(7) 2 C (in Fe, 1%) + O₂ (g, 1 atm) = 2 CO (g, 1 atm)
$$\triangle F^{\circ}_{175515070\%} = -83,120-11.0T$$

(8) Si (in Fe, 1%) + O₂ (g, 1 atm) = SiO₂ (s, act 1)
$$\Delta$$
F°₁₇₀₀ 1970 κ = -185,300 + 47.7T

(9)
$$2Mn$$
 (in Fe, 1%) + O₈ (g, 1 atm) = $2MnO$ (s, act 1) $\Delta F^{\circ}_{1500 \text{ mos}1^{\circ}\text{K}} = -212.920 + 72.02T$

The major tonnage of steel castings poured in this country have an approximate composition of 0.30 per cent carbon, 0.50 per cent silicon and 0.80 per cent manganese. When the actual activities of the elements are used in place of unit activities, the reactions become:

(10) 2 G (in Fe, 0.3%) + O₂ (g, 0.21 atm) = 2 CO (g, 0.01 atm)
$$\triangle$$
F = -83.120-22.2T

(11) Si (in Fe, 0.5%) + O₂ (g, 0.21 atm) = SiO₂ (s, act 1)
$$\triangle$$
F = -185,300 + 52.2 T

(12) 2 Mn (in Fe, 0.8%) + O₂ (g, 0.21 atm) = 2 MnO (s, act 1)
$$\Delta$$
F = -212,920 + 76.0 T

(13) Fe (1, act 1) + O₂ (g, 1 atm) = 2 Fe⁰ (1, act 1)

$$\triangle F^{\circ} = -111,250 + 21.67 \text{ T}$$

The pressure of oxygen necessary to be in equilibrium at 1808 °K, with each element at its respective activity and the oxidation product at the activity given above, is as follows:

| Carbon | Pop : | = | 1.27 | x | 10-15 | atmospheres |
|-----------|-----------------|---|------|---|-------|-------------|
| Silicon | Po ₂ | _ | 8.9 | x | 10-18 | atmospheres |
| Manganese | Po2 : | = | 6.3 | x | 10-10 | atmospheres |
| Iron | Po. | _ | 1.9 | x | 10-9 | atmospheres |

The CO pressure of 0.01 atmospheres is assumed. If the CO pressure is less than 0.01 atmospheres, the equilibrium oxygen pressure at 1808° K is even less than 1.27×10^{-15} atmospheres. The outside layer of the casting loses carbon, silicon and manganese before the iron oxidizes. One factor determining the thickness of this layer is the rate of diffusion of the element outward or the oxygen inward. The reaction $O_2 + (C, Si, Mn) = (C, Si, Mn)_x O_y$ will occur preferentially to $O_2 + 2$ Fe = 2 Fe O. However, the iron oxidation can occur thermodynamically. From experience FeO does form, so the depletion rate of C, Si and Mn exceeds the replacement rate by diffusion.

Discussion of Results and Conclusions

Work done to date on mold-metal interface reactions is only a small beginning; much work remains before the specific causes of the various surface defects in steel castings can be isolated and remedies established. Variables of shop practice are so numerous and uncontrollable that our knowledge can never be developed empirically. Only by systematic laboratory investigation will it ever be possible to progress much beyond the current state of the "art." It is hoped that many such researches will go forward to prepare the way for dependable productions of smooth, blemish free, steel castings. The writers believe this is a

fair and attainable goal.

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This paper provides dependable information on the nature and end products of interface reactions. The findings have been surmised for some time but not definitely proved. Specifically the conclusions which may be drawn are-

1. The end product of the interface reaction between the steel casting and the silica sand mold is fayalite (2FeO.SiO₂) -nothing else. This is shown by x-ray diffraction analysis of shop and laboratory samples.

2. The mechanism for forming fayalite is oxidation of the iron of the steel and subsequent reactions of this oxide with SiO2.

3. Interface reactions do not occur in other than

oxidizing atmospheres.

4. A useful laboratory method for studying interface behavior is described; it consists of a pin embedded in a sand test cylinder, with the pin heated to melting by induction.

Acknowledgment

This work was made possible by funds provided by the Illinois Clay Products Co. and by a scholarship given by the Steel Founders' Society of America, a member of the Foundry Educational Foundation. The authors hereby express appreciation for these

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DISCUSSION

Chairman: J. JUPPENLATZ, Lebanon Steel Foundry, Lebanon,

Co-Chairman: C. H. LORIG, Battelle Memorial Institute, Co-

CHAIRMAN JUPPENLATZ (Written Discussion): Messrs. Savage and Taylor's studies on the interface reaction between the steel casting and the silica sand mold are commendable contributions to a better understanding of steel foundry practices. As indicated by the paper, the fayalite product at the interface is aggravated in intensity as the molten steel mass increases producing higher temperatures over sustained time periods. This is of practical importance to the steel foundryman as well as the equilibrium diagram, Fig. 8, illustrating the theory of fayalite

In commercial practice, the effects of the fayalite formation

are easily observed on production castings. There is no doubt about sand mold interface breakdown. It is now shown, that liquid fayalite is present after casting and exists at temperatures above 2200 F. Steel is usually poured at 2800 to 3000 F, releasing FeO which in turn, is very reactive with silica forming fayalite, diffusing under the mold surface. We then have a mold that is covered with a liquid phase at temperatures above 2200 F. At this same time, metal pressures are the greatest, pushing the metal skin of the casting, and possibly liquid metal, into the fayalite liquid. The resulting casting surfaces exemplify these

It would be logical to assume that upon the production of fayalite, that physical and dimensional changes would occur on the mold face and that fusion would cause a larger mold cavity. Have tests been made to indicate the true and apparent densities of fayalite, and if so, how do the results compare with silica sand

The authors have studied effects of atmosphere on fayalite reactivity, concluding that oxygen is necessary for its reaction. They suggest that carbon or graphite might be incorporated in the sand to produce a CO atmosphere. This practice has been employed to a limited extent without completely satisfactory results. Molten steel has an extremely high affinity for carbon. If carbon is present in significant quantities, in the sand or wash, rough penetrated surfaces prevail. Often sufficient gas pressures are generated which prevent the molten metal from lying quietly on the mold surface, producing castings with excessive crater marks and dirt inclusions which are less desirable than the results of fayalite reactions. We do normally employ the addition of cereal binders (organic) in sand mixtures to promote binding qualities. Indirectly, these carbonaceous organic compounds create an atmosphere of CO upon casting, which in turn, assist in reducing fayalite formation and are considered to improve peeling and cleaning characteristics.

A more practical approach toward the improvement of steel casting surface qualities seems to be resolved in the use of an inert refractory wash, applied to a silica mold backing that is not reactive to FeO which is present under normal atmospheric casting conditions. Research toward this goal should be given high priority, and if successful, should provide significant reductions of production costs along with improved steel casting

H. B. Myers (Written Discussion): 1 Doctors Savage and Taylor are to be highly complimented on the stimulating and thought provoking paper which they have presented. The problem of "penetration," "burnt on sand" or "adhering sand" has been with the steel foundrymen for many years, and it now appears that the solution is close at hand.

Since the early work of Dierker1 and Goodale,9 many theories relative to the mechanism of adhering sand have been proposed. In later years, however, two factors have been recognized: (1) mechanical penetration, and (2) fusion, together with chemical

Evidence that the "shink gray cake" which peels off a steel casting in the shakeout operation is fayalite, was offered in a report on the work at the University of Sheffield by Bishop and Lewis, published in the Foundry Trade Journal, Feb. 10, 1949. It is interesting to note that in petrological and X-ray diffraction studies, they found no evidence of free iron oxides, but that the major constituent of the slag was "fayalite, the ortho-silicate

The present work of Savage and Taylor not only confirms the foregoing but establishes many new facts as well. By means of an ingenious laboratory test, which they developed, it is now possible to study the effect of various compositions of liquid steel at various temperatures in contact with various mold materials for various times under various atmospheres

It can be noted in the paper that the effect of elements dissolved in the steel on interface reactions is considered. I should like to ask the authors whether they have had the opportunity to observe the effect which residual aluminum may cause?

During the past war, I was associated with a steel foundry equipped with both acid open hearth and acid electric melting facilities. Castings of the same pattern were often poured from either open hearth or electric steel. It was observed that the certain design castings would be plagued with adhering sand when poured in acid electric steel; whereas, castings from the

¹ Technical Director, Roll Manufacturers Institute, Pittsburgh, Pa.

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same pattern poured from acid open hearth steel would have smooth sand-free surfaces.

The first thought was that there was a difference in pouring temperatures—the electric steel being hotter. However, subsequent temperature measurements and controlled pouring temperature experiments disproved this idea.

The second thought was of deoxidation practice. The acid open hearth steel was deoxidized with aluminum in the order of none to 4 oz per ton; whereas, the acid electric steel was deoxidized with aluminum in the order of 2 lb to 6 lb per ton.

Could it not be that the higher residual aluminum content in the acid electric steel was effectively preventing or at least limiting the FeO formation and the subsequent fayalite reaction, thus giving rise to tightly locked sand grains on the surface of the casting? This may be somewhat analogous to the data presented by the authors to show how the interface product forms. The casting which was "bled" in 30 sec had sand grains so tightly locked to the casting surface that sand blasting did not remove them. In this case the fayalite reaction was limited because of heat limitations—temperature and time at temperature. It would seem logical that residual aluminum could also limit the fayalite reaction by limiting the amount of FeO which is formed (in fact, the authors state that C, Si, and Mn retard the fayalite formation).

In light of the authors' investigation, several factors come to mind in considering the problem of "adhering sand":

- 1. Metal
 - a. Composition (per cent and activity of easily oxidizable elements)
 - b. Pouring temperature
- 2. Casting Design
 - a. Section size
 - b. Reentrant angles and pockets
 - c. Gating and length of metal run
- 3. Mold Materials
 - a. Compositions and chemical properties of various constituents
 - b. Grain sizes of various constituents
 - Heat conductivities and thermal capacities of various constituents

The foregoing factors are all somewhat inter-related, in that they govern the initial temperature, the time at temperature, and the chemical reactions which will take place. Perhaps a balance exists between these factors where adhering sand no longer occurs.

From the work presented, it would appear that there are two ways to arrive at a smooth sand-free surface on steel castings.

- To exclude oxygen from the mold atmosphere
 To control the favalite reaction so that just enough form
- To control the fayalite reaction so that just enough forms to cause a fragile, easily removed "sinter-layer."

With reference to the first method, would the pressure developed by the liquid metal in a 2000-lb casting be sufficient to cause pure mechanical penetration (assuming an inert atmosphere)? With reference to the second method, perhaps a mold wash composed of synthetically-made fayalite or possibly acid open hearth slag may prove helpful in those cases where fayalite does not readily form; that is, where small sections cause time and thermal limitations, and/or dissolved elements cause chemical limitations.

It would be interesting to know whether the authors have investigated the interface reaction products formed when zirconite sand is used.

In conclusion, I would like to say that the authors have made a valuable contribution to the steel foundry industry, and it now seems likely that the problem of "adhering sand" can be solved.

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 J. B. CAINE (Written Discussion): 2 As long as the authors stick
- J. B. Caine (Written Discussion): ² As long as the authors stick to their investigation of the composition of the sinter layer, all the writer can say is "yes sir." However, throughout the paper the authors flirt with the implications of the favalite reaction

and the sinter layer on casting quality. It is on these flirtations that this discussion is based.

In paragraph C the authors discuss the formation of the sinter layer and draw the conclusion that the interface reaction is time dependent. It is time dependent as far as the amount of sinter, but so is any thermo-chemical reaction occuring in a heat gradient. If the glaze in the dip test is the product of the same interface reaction discussed in this paper, the interface reaction occurs almost instantaneously, in less than 2 sec. A more sensitive test would undoubtedly show that the reaction occurs instantaneously.

Another point that has not been investigated is when the sinter layer forms. The dip test has shown that a glaze forms on bentonite-bonded sands such as studied by the authors, when the sand is in contact with liquid steel, but there are significant differences. First the glaze in the dip test is white, the sinter layer is black. Second, the thickness of glaze in the dip test never approaches the thickness of the sinter layer as sketched in Fig. 9B, even at longer time in contact with liquid steel than that of the sand in Fig. 9B. This brings up the important point that possibly the sinter layer found on steel castings on shakeout is formed after the surface has solidified. If this supposition is true, then the sinter layer can have no effect on those defects, scabs, buckles and eroded sand, that must form when the sand is in contact with liquid steel.

The footnote on page 568 implies that the slowness of the fayalite reaction is a possible cause of sand sticking onto small thin castings. This statement contradicts the combined efforts of the Sintering and Mold Surface Committees to find any cause for sand adherence other than metal penetration into the sand. Every case of sand adherence on small castings investigated was due to too large voids in the mold face and metal hot enough to penetrate these voids before freezing off, plus the fact that the surface finish requirements are stricter for small castings than for large.

The authors state in Paragraph E that a reducing atmosphere would be advantageous. This does not check with the results of the dip test and practical experience. Certain glazes or sinters such as are formed on bentonite-bonded steel molding sands are beneficial as far as resistance to erosion and veining are concerned. Pure carbon in the form of gilsonite added to green sand facings will slow down the glaze formation appreciably and increase the tendency to erosion and veining just as appreciably.

On the other hand, increasing the glazing tendency too much, with strong fluxes such as sodium and calcium compounds seem to be detrimental. The authors have shown that glazes and sinters are the end products of a fayalite reaction. It is hoped that more work will be done along this line, for sinter or glaze formation may be surprisingly important.

B. RICHARDSON: What is the smallest weight per cent of a constituent in the sample detectable in the x-ray method of analysis used in this work?

C. W. Briggs: 4 In the research that the Steel Founders' Society carried on in connection with reclamation of steel foundry sands we also found fayalite and recognized it. We soon found it was of little concern to us in the reclamation of sands because for the most part the fayalite stays with the casting regardless of casting size and goes out in the refuse after blasting, so that it really does not enter much into the sand reclamation system.

C. E. SIMS It will be observed on the phase diagram that fayalite (2FeO-SiO₂) is the only compound formed between FeO and SiO₂. Naturally, it would be the mineral most likely to be found. There is, however, quite a range of nonstoichiometric silicate glasses which would not be indicated by X-ray analysis. Some of these should also be present.

The impression seems to have grown in this discussion that fayalite is altogether a desirable thing to have present, because it helps to obtain clean castings. There is a suspicion, however, that it may also act as a flux to cause the steel to "wet" the sand and thus cause severe penetration at times. I wonder whether the authors have found any evidence of that?

CHAIRMAN JUPPENLATZ: It is my impression too that fluxing may be very undesirable.

² Technical Consultant, Wyoming, Ohio.

³ Research Engineer, Steel Castings Institute of Canada, Ottawa, On-

⁴ Technical and Research Director, Steel Founders' Society of America, Cleveland.

⁵ Assistant Director, Battelle Memorial Institute, Columbus, Ohio.

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MR. SAVAGE: We should like to thank Mr. Myers for his very welcome comments on this paper. We have made tests with mold washes and facings of synthetically prepared fayalite and the results were extremely encouraging. Fayalite wash was swabbed on the mold of a 10-lb cylindrical casting. The fayalite wash was synthetically made in the laboratory previous to its application on the mold. A second mold was rammed with no wash for control purposes.

The two steel castings were poured from the same ladle. The sand on the casting with no wash could not be removed with a chisel and hammer. The sand had burned in very badly. However, the sand on the casting with the synthetic fayalite wash could be removed by inserting a fingernail between the casting and the mold. The sand peeled easily from the casting in a few large pieces. The surface finish was excellent on the casting

made in the mold coated with synthetic fayalite.

We believe this test should open a field for much more extensive study. At present, we should not decide whether fayalite is desirable or undesirable, because we do not have sufficient information on this subject. After we get enough facts about fayalite washes, we can then evaluate the results and make a decision about the desirability of fayalite. Research men should consider the possibility of fayalite washes in future investigations of mold washes.

Zirconite sand, composed principally of the compound zirconium silicate, does not appear to break down at steel pouring temperatures. We made X-ray diffraction studies of zirconite sand at room temperature and after having melted steel next to the sand. There is no noticeable difference between the X-ray patterns of zirconite sand next to the casting before or after pouring steel.

The best way to determine the effect of penetration from the ferrostatic pressure developed in a 2000-lb casting would be to try it. Several factors which affect mechanical penetration are the sand grain size distribution and the mold hardness.

The comments about aluminum retarding fayalite formation appear basically correct. Undoubtedly, the aluminum would preferentially combine with the oxygen before the iron would combine with the oxygen. Therefore, less iron oxide would be formed with subsequently less fayalite.

With reference to Mr. Juppenlatz' questions, we have no data on the relative densities of fayalite or iron. Probably the density

of fayalite is less than the density of iron.

I am not certain as to what Mr. Juppenlatz means by his second question about carbonaceous material. However, if the formation of fayalite can be entirely prevented by carbonaceous material, the surface finish should be satisfactory. This was shown in the illustrations of tests made in reducing and inert gases. Under these conditions, there is no oxidation and no fayalite reaction. The casting surface was satisfactory.

Mr. Sims was asking whether we noticed any glassy slags which formed by reactions between iron oxide and silica. The answer is yes. If we heat silica sand to high temperatures and cool quickly, we do not get a crystalline pattern by X-ray diffraction study. The pattern obtained is characteristic of a vitreous or glassy material.

We cannot say at present whether fayalite formation is desirable. Fayalite is a low melting point compound and it may possibly wet the sand and cause penetration. The amount of fayalite formed or used as a mold wash must be considered. It is an important factor to be considered in evaluating the

amount of penetration.

DR. TAYLOR: This is strictly an exploratory proposition and no one should feel that the presence of fayalite is either good or bad. It is there. Now it is up to you to find out what to do with it. This paper does not attempt to do that. However, I can conceive of conditions where it can be good and conditions where it could be bad, each depending upon the mechanical arrangement of the sand grains. For example, in one case if your mechanical penetration is eliminated by closing up your sand, that is one story. If your sand grains are large and the structure is open, then surely a fluxing action is going to lead the molten metal and oxide between and around sand grains. I think that was mentioned in the paper as a possibility. So the whole thing is quite complex. This is only a small beginning.

It was observed by Mr. Myers, Mr. Briggs and others that this ghost had already been laid; all of us surmised that the reaction product was fayalite, that it had to be, but nobody proved it. This is the proof. It adds nothing particularly new to the picture. About a month after co-author Savage finished his work, the British came out with their paper—this was in February and they also proved the presence of fayalite. I do not think our paper mentioned this as a reference because at the time we did

our work it had not been published.

I do not agree with Mr. Briggs that all the fayalite is in the cleaning room. I would guess 5 per cent might be.

Mr. Briggs: About 90 per cent.

DR. TAYLOR: This layer is sometimes 1/8 in. thick on small castings; on large castings it gets to be 2 in. thick, and most of that layer is fayalite. I consider this reasonably important, and that it does not end up in the cleaning room. Aside from the effect on the used sand, the extent of the fayalite reaction influences surface quality of castings. It should be understood and carefully controlled.

SUBCRITICAL ISOTHERMAL GRAPHITIZATION

By

H. A. Schwartz, J. D. Hedberg and R. Eriksen*

ABSTRACT

This paper points out that the rate of secondary graphitization in a white cast iron cannot be defined without first defining the conditions of an intermediate stage of graphitization going on during the cooling process.

PRINCIPLES of so-called first stage graphitization have been completely elucidated. It was possible to describe the mechanism quantitatively in a paper by Schwartz, "The Kinetics of Graphitization in White Cast Iron," published in *Transactions*, ASM, vol. 30, p. 1328 (1942). The quantitative relations are complex although the experimental conditions of the study, involving only time at constant temperature, are simple.

A parallel experiment carried out at subcritical temperature would be equally simple and could be evaluated by mathematical methods similar to those used in the study of first stage graphitization. It would, however, have only academic interest for no operating importance attaches to a process carried out entirely below the critical point. The time involved in completion of the reaction is prohibitively great. What is required is a study of the completion of graphitization, below A_1 , of a material which has been brought to equilibrium above that temperature.

However, this is not a single process as implied by the phrase "second stage annealing," but consists of two processes; cooling to a subcritical temperature and holding at an approximately constant subcritical temperature. Schwartz and Barnett discussed what happens during cooling in "Conversion of Austenite to Ferrite and Cementite," in Transactions, A.F.S., vol. 49, p. 441 (1941). The title was poorly chosen as it does not indicate that both cementite and graphite are formed and the formation of the latter rather than the former is the feature of practical interest to the malleable foundryman. The data are, however, there available.

It thus appears that the annealing process as a whole does not consist of two stages but of three; there being an intermediate stage in the cooling interval where graphitization is extremely rapid during a short time interval. What is required of the final "second" stage, and what happens in that stage is, therefore, dependent on what happened in the "intermediate" stage. Neglect to separate the intermediate and second stage effects has rendered all previous investigations by ourselves and others of little value in elucidating what goes on in the "second" (constant subcritical temperature) stage. Yet this is of major operating importance unless the commercial process is so conducted as to be complete in the intermediate stage when A₁ stable is reached.

The simplest mode of attack is to suppress the intermediate stage by sufficiently rapid cooling from supercritical to subcritical temperature. Actually this condition can be closely approached but hardly reached. One may then note the modifications of what has been observed under these simple conditions when, as the result of slower cooling there has been some transformation in the intermediate range, resulting in "bull's eyes" of ferrite around the graphite when the subcritical temperature is attained.

The conditions of first-stage graphitization will also effect second-stage graphitization by influencing nodule number. The experiments should, therefore, be planned to illustrate the effect of graphite nodule number, ferrite zones around graphite and the metallography of the agraphitic (combined) carbon, whether pearlite or martensite; all in the sample as the isothermal "second" stage of anneal begins.

The experimental material was a white cast iron of Cleveland Works' manufacture, available as the gage length of the conventional ASTM malleable test bars. All the pieces came from a single ladle of liquid iron. The composition was as follows:

TABLE 1-COMPOSITION OF TEST PIECES

| Element | Per Cent |
|------------|----------|
| Carbon | 2.52 |
| Silicon | 1.06 |
| Manganese | 0.54 |
| Phosphorus | 0.130 |
| Sulphur | 0.139 |
| Chromium | 0.025 |
| Boron | 0.0015 |
| Copper | 0.09 |

^{*} National Malleable & Steel Castings Co., Cleveland

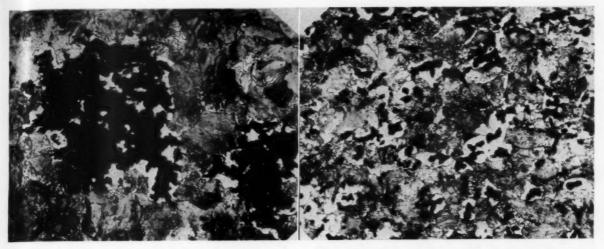


Fig. 1-Structure at 500x resulting from heat treatment "a." Nital etch.

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Fig. 2-Structure at 500x resulting from heat treatment "b." Nital etch.

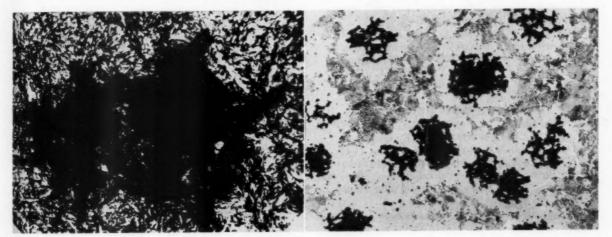
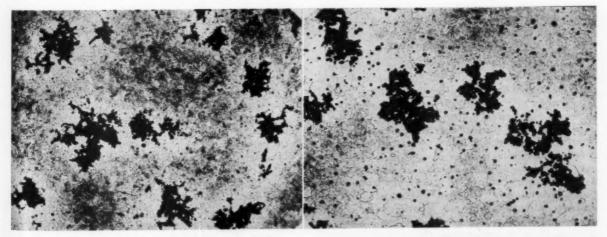


Fig. 3-Structure at 500x resulting from heat treatment "c." Nital etch.

Fig. 4-Structure at 100x resulting from heat treatment "e." Nital etch.



ment "a" plus 8 hr at 680 C. Nital etch.

Fig. 5-Structure at 100x resulting from heat treatment "a" plus 24 hr at 680 C. Nital etch.

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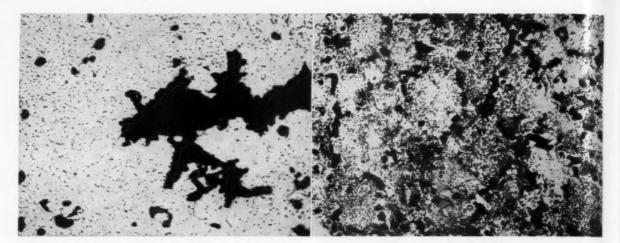


Fig. 7-Structure at 500x resulting from heat treatment "b" plus 2 hr at 678 C. Nital etch.

Fig. 8-Structure at 500x resulting from heat treatment "b" plus 16 hr at 680 C. Nital etch.

The material was given various first and intermediate stage heat treatments as follows:

- (a) Hold 48 hr at 900 C (1650 F) and cool on a brick in boat to room temperature.
- (b) Hold 5 min at 1000 C (1830 F); quench in water, then hold 7 hr at 900 C (1650 F) and cool on a brick in boat to room temperature.
- (c) Hold 48 hr at 900 C (1650 F); quench in water.
- (d) Hold 48 hr at 900 C (1650 F); cool in 45 min to 800 C (1475 F) then cool at 15 C (27 F/hr) to or below 690 C (1275 F) and air cool.
- (e) Same as (d) except cooled at 7 C/hr (12 F/hr).

Throughout these and subsequent heat treatments, surface oxidation was minimized by having present in the closed quartz tube with the specimens, a considerable amount of white cast iron crushed to a powder to offer greater surface for reaction with the limited amount of air originally in the tube and thus to establish gas-metal equilibrium without significant effect on the samples.

Metallographic Examination

Each separate specimen was examined microscopically to make sure that the specimens from a given heat treatment were substantially similar. Typical micrographs at 100 diameters and 500 diameters are shown in Fig. 1 to 9.

The isothermal subcritical heat treatment consisted of holding at 680 C (1260 F) for various times appropriate to obtaining a series of specimens varying in agraphitic carbon in steps suitable for defining a time-agraphitic carbon curve. We use Henry Howe's term "agraphitic carbon" to mean carbon which is not graphite, without making any assertion as to whether or with what it is chemically combined. There is little doubt in the present case that it is all present as cementite. With heat treatment (a), 635 C (1175 F) was also used.

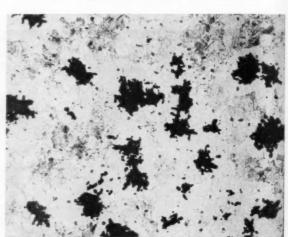


Fig. 9-Structure at 100x resulting from heat treatment "d" plus 4 hr at 680 C. Nital etch.

Difficulties were encountered in analyzing malleable by the method described by J. G. Donaldson of Battelle Memorial Institute in "Direct Determination of Combined Carbon in Cast Iron and Steel," TRANSACTIONS, A.F.S., vol. 50, p. 931 (1943).

That method works perfectly in the absence of graphite and also on highly graphitic gray iron samples. It has been observed, from time to time, to give ludicrously high values on malleable irons which are nearly completely annealed. There is much to be said in favor of a direct method and against the separate determination of total carbon and graphite, obtaining the combined carbon by difference, as was done in this investigation. We have since overcome this difficulty by an expedient described in "Note on Donaldson's Method for Determining Combined Carbon in Malleable Iron," AMERICAN FOUNDRYMAN, vol. 16, no. 3, p. 53, Sept. 1949. A major difficulty in the difference method for determining combined carbon is the preparation of a sample from which individual samples, uniform as to their very fine graphite content, can be weighed.

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For this study not the absolute values of but only the differences in the total and graphitic carbon were important. The sample therefore, if uniform, need not contain the same amount of graphite which the material as a whole contains. The sample preparation adopted was as follows:

After the final heat treatment, the outer surface of the specimen, which was 5% in. in diam, was removed to a depth of 0.05 in. to 0.06 in. in order to avoid traces of pearlitic rims or traces of contamination by oxides. The sample for analysis was then removed in the form of medium-sized chips or turnings. These turnings were sieved, retaining only what was held in a 40-mesh sieve. The residue was washed by decantation with water, then once with hot 10 per cent NaOH, accompanied by vigorous agitation, again with water and finally with boiled distilled water, after which it was dried in vacuo. The residue was again sieved to prepare sample for chemical laboratory.

With these precautions it was possible to reduce the error in determination of combined carbon. Numerous combustion determinations were, therefore, made, the number depending on the degree of uniformity obtained. Determinations were always made in duplicate and sometimes on as many as six individual samples. These results are believed to be the best values for combined carbon that can be reported while still admitting the possibility of minor errors.

First-Stage Heat Treatment

Specimens representing the various first-stage heat treatments (see p. 3), were treated as already described for various times at approximately 680 C (1260 F) and approximately 635 C (1175 F). It will presently appear that great temperature precision is not required. The temperatures were probably within 2 C of those here mentioned.

Various time values were chosen for different specimens in any given preliminary treatment to obtain a suitable sequence of combined carbon contents.

Because the values for combined carbon are not too precise, there seemed to be no need to attempt refined methods of curve fitting to express the combined carbon-time relation. Instead, the combined carbon content was plotted on a logarithmic scale against time on a linear scale and the relation was represented by the best straight line which could be obtained visually.

This does not imply, particularly at this stage, that the relation is actually logarithmic, nor should it be deduced that the process is governed by a first order or monomolecular reaction. This may or may not be so. The logarithmic form of plotting was, however, a convenience in analyzing the results.

Actual data are shown in Fig. 10, in which the ordinates are combined-carbon values (not to be confused with logarithms of the values for combined carbon or with carbon on a logarithmic scale), and the abscissae are time values. Curves are plotted from the original logarithmic graphs representing the best relation of combined carbon to time for each heat treatment, assuming the particular form of relation which has been adopted, with or without good reason.

A major reason for adopting the logarithmic rela-

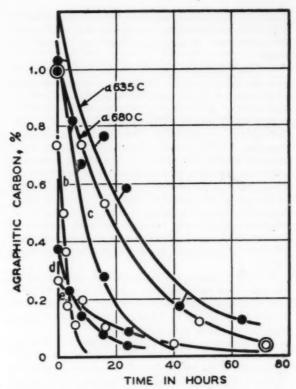


Fig. 10—Curves show relationship between time at temperature and percentage of agraphitic carbon in the iron.

tion, since it represents the points about as well as any other relation, is that it furnishes a simple method of determining graphitizing rate. It is a characteristic of this relationship that the fraction of the total amount of combined carbon present at the beginning of a given time interval which is destroyed in that given time interval is the same no matter where on the curve the measurement is made. This same relationship pertains to the decay of radium and other radioactive substances and the physicists speak of a property called "half life," which is the time interval during which half of the material will have been destroyed. This expedient was adopted in expressing graphitizing rates.

Table 2 gives the half lives of combined carbon for the various heat-treating conditions used in the investigation.

TABLE 2

| Preliminary Heat Treatment | Tempera Final Heat | Half Life, hr | |
|-------------------------------|-----------------------|------------------|-------|
| | F | C | |
| a | 1260 | 680 | 141/2 |
| a | 1175 | 635 | 13 |
| b | 1260 | 680 | 11/2 |
| С | 1260 | 680 | 7 |
| d | 1260 | 680 | 7 |
| e | 1260 | 680 | 141/2 |

It is observed that the "half lives" in the first and second lines of Table 2 are practically the same, indicating that in this temperature range there is prac-

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tically no thermal coefficient of the reaction velocity. This is fortunate because it indicates that small errors of temperature measurement have not affected the results.

It is a principle of thermodynamics that the velocity of a process is proportional to the heat of activation of the process. This may either be the heat of the activation of a diffusion process or the heat of reaction. Quite usually the heat of activation of a diffusion process is much less than that of a chemical reaction. In the present case this would not be so marked because the heat of formation of cementite (which is negative), or correspondingly the heat of dissociation of cementite into graphite and iron (equal to the former and positive) is small. Since one is confronted with a meaningless but negative thermal coefficient, one can only say that the heat involved in the process is small and cannot be used to determine whether the process is one of diffusion or one of dissociation.

In working above the critical point the thermal coefficients bore some sensible relation to the diffusion rate of carbon as measured at various temperatures by others. This cannot be translated into a system where iron in the alpha state is involved.

The enormous difference in half life between the first and third lines (Table 2) is due to, or at least accompanied by the expected enormous increase in number of graphite nodules, an increase which is well known to be produced by the prequenching treatment. This suggests that graphitizing rates are concerned with a migratory or a diffusion phenomenon rather than with chemical dissociation.

The relatively short half life in the fourth line of Table 2, accompanying quenching from 900 C, is not easily accounted for by considering diffusion alone. There is no marked increase in nodule number since the nodules were formed at 900 before quenching took place. Why diffusion should be better with a martensitic matrix is not clear up to this point.

The metallographic aspects are illustrated by micrographs. Figures 1 to 9 inclusive were prepared to follow metallographic changes, but as a matter of convenience and available material, they are not of the identical iron and heat treatment so far discussed. This is of no consequence since no specific quantitative conclusions are drawn. The micrographs were used purely as illustrating successive steps in a progression. They were selected from available specimens in the files which parallel the present investigation. All specimens were etched with nital in developing the microstructures.

Microstructures Interpreted

Figure 1, at a magnification of 500 diameters, represents the iron with preliminary heat treatment "a." The high magnification was selected to show that the segregation of ferrite around the temper carbon nodules were almost completely suppressed, and that the metallic matrix is a rather fine-grained, but nevertheless, laminated pearlite.

Figure 2 represents the result of preliminary heat treatment "b" at the same magnification as Fig. 1. It is at once evident that the nodule size has decreased

enormously and the nodule number increased enormously. The prequenching has so far accelerated secondary graphitization that there is rather more ferrite surrounding the nodules than before. The matrix structure is, however, quite similar to that following heat treatment "a."

Figure 3 represents first-stage heat treatment "c," again at 500 diameters. It will be observed that the nodule is large and that the background, while perhaps not truly martensitic, is somewhat similar to that structure.

Figure 4 represents heat treatment "e." The magnification is only 100 diameters in order to include enough of the field to show the formation of a ferrite bull's eye and the rather coarsely laminated pearlite matrix.

Second-Stage Heat Treatment

The remaining micrographs illustrate the effect of second-stage heat treatment on the structures so far described.

Figure 5, at a magnification of 100 diameters, represents a white iron given preliminary heat treatment "a" and subsequently held 8 hr at 680 C. It will be noted, first, that the lamination of the pearlite has largely disappeared, the matrix having become spheroidized; and second, that the beginning of bull's eyes around the temper carbon have become more prominent.

Figure 6 is similar to Fig. 5 in all respects except that the heat treatment at 680 C was prolonged to 24 hr. At first glance one sees a multitude of fine particles. Too much attention should not be paid to these for close scrutiny of Fig. 5 shows some of these buried in the spheroidized matrix. The ferrite bull's eyes have increased in size but are not at all sharply defined in outline and the character of the matrix is similar to that shown in the preceding figure.

By combining Fig. 1, 5 and 6 together it seems evident that the ferrite zone around the temper carbon expands, the temper carbon of the matrix presumably has migrated to the nodule and the matrix becomes spheroidized relatively early in the heat treatment.

Figure 7 represents material given heat treatment "b" and subsequently held 2 hr at 678 C. In Fig. 7, a growth in ferrite areas, the development of ill-defined boundaries in the ferrite areas and a partial spheroidization of the matrix are evident. Some evidence of the prior laminated arrangement of the carbide persists among the spheroids of the matrix.

Figure 8, at a magnification of 500 diameters, represents a material previously given heat treatment "c," and subsequently held 16 hr at 680 C. It should be noted that the matrix has become completely spheroidized and that there is relatively little evidence of any ferrite bull's eyes, although ferrite grain boundaries are visible, especially in the region of the large temper carbon nodule. One is inclined to think, however, that the spheroids are more thickly scattered away from the temper carbon nodule than near it.

Figure 9, at a magnification of 100 diameters, represents heat treatment "d," followed by holding for 4 hr at 680 C. (Actually the cooling rate of this specimen corresponding to "d" instead of "e" is of no

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consequence in deciding upon the general character of the metallographic structure.) The prior laminated arrangement of the carbide is only slightly in evidence and the boundaries of the ferrite bull's eyes retain their sharpness. It is quite likely, so far as the present investigation discloses, that material which has been slowly cooled from the temperature of first-stage graphitization to the temperature of second-stage graphitization, characteristically has sharply delineated bull's eyes. This, in turn, may be of some importance in determining graphitizing rates.

The micrographs seem to indicate that migration plays an important part in the graphitizing process. Although there is evidence of the migratory rate, however, it is possible, though unlikely, that migratory rate is not the process determining the rate of secondary graphitization. This paper points out that the rate of secondary graphitization in a white cast iron cannot be defined without first defining the conditions of an intermediate stage of graphitization going on during the cooling process.

DISCUSSION

Chairman: H. C. STONE, Belle City Malleable Iron Co., Racine,

Co-Chairman: E. Roby, Jr., Peoria Malleable Casting Co., Peoria. Ill.

PROF. RICHARD SCHNEIDEWIND:1 Dr. Schwartz always can be counted upon to present a valuable and informative paper. There are several significant points which seem important.

In one photomicrograph Dr. Schwartz showed that the structure of irons cooled from above the critical temperature was different in areas immediately in contact with the temper carbon than in areas farther away. We have quenched partially annealed white irons into water from 1800 F and obtained coarse martensite needles in the matrix and in contact with cementite. However, the martensite needles in immediate contact with temper carbon were quite fine.

In this case there are but two explanations: the solubility of carbon in austenite is lower in contact with the graphite phase than in contact with the carbide phase. The differences in concentration of carbon could be responsible for the differences in martensite needle size. Another possibility is the stress concen-

trations around the temper carbon nodules.

I agree with Dr. Schwartz that it is unwise to discuss the kinetics of second stage graphitization on the basis of laboratory tests when the history of the iron during first stage and in cooling to the second stage is not completely described. Second stage graphitization studies in the laboratory on samples which have been cooled in air or water from the soaking temperature only give comparative results. The operating foundryman has recognized the value of not dropping to below the critical temperature rapidly but cools his iron slowly through the 3-phase region for most efficient results.

J. E. REHDER (Written Discussion): This interesting paper adds another to the long list of contributions to the metallurgy of malleable iron from the laboratory of Dr. Schwartz. The present writer finds himself in complete agreement with the authors in their statement that study of isothermal subcritical graphitization must take into account the changes inevitably taking place when a sample of iron is cooled through the critical temperature range from the first stage or soaking temperature to a subcritical holding temperature. The paper provides useful data on the relationships between time of holding and the amount of graphitization taking place at subcritical tempera-

The mention of indication that migratory or diffusion rates play an important part in the graphitization process is in agreement with the position of the present writer, except that in the writer's opinion, as stated previously in discussions, the migratory or diffusion rate of silicon, rather than that of carbon is the controlling factor.

Two other points may be mentioned. In the description of the various heat treatments given the samples, under "e" is described a continuous slow cool through the critical temperature range. No further mention is made in the paper of the samples or results from this heat treatment, and the reason for

including it is obscure.

The authors of the paper mention that the photomicrographs shown are not of the specimens used in the experimental work, but are "illustrative," and they then devote considerable space to a detailed discussion of features of the microstructures. It is considered unfortunate that micrographs of the actual specimens used were not prepared, since the value of the discussion of the structures would thereby be greatly enhanced.

Dr. Schwartz: In answer to Mr. Rehder, the reason we did not show a photomicrograph of sample subjected to heat treatment "e" or discuss it further is because microstructure of this sample differed so slightly from that of sample subjected to heat treatment "d" as to be quantitatively indistinguishable. It makes

no difference which one is shown.

Since we raised the comment about the inapplicability of Donaldson's combined carbon method, I should like to add that we found what was wrong with it and how to remedy it. Our findings have been published in a paper "Note on Donaldson's Method for Determining Combined Carbon in Malleable Iron' by H. A. Schwartz and G. M. Guiler, AMERICAN FOUNDRYMAN,

vcl. 16, no. 3, Sept. 1949, p. 53.

The curves shown in the present paper represent a graphite the amount present being proportional to the 11/2 power of time. This is the type of curve one gets in the early stages until the point of contact between the ferrite spheres is reached so that you no longer have the completely spherical surface enclosed in the combined carbon. Until we get that, the migratory rate is to the 11/2 power of time. Other investigators pointed this out as the criterion of the migratory rate phenomenon. I am withholding comment on the migration rate of the silicon until I see some evidence on it. So far, I won a case in the Federal Court in part on the claim that there was no migratory rate of silicon.

¹ Prof. of Met. Engr., University of Michigan, Ann Arbor.

⁸ Foundry Engr., Canadian Bureau of Mines, Ottawa, Ontario, Canada.

NATURE OF MOLD CAVITY GASES

By

Charles Locke* and Richard L. Ashbrook**

ABSTRACT

Variables affecting pressures which develop in a mold cavity during the pouring of a casting were studied.

These pressures were found to increase directly with increase of pouring rate and inversely with permeability. Cereal content of the molding sand had an inappreciable effect on the mold pressure. Intensity of the pressures observed were all insufficient to have an appreciable effect on filling of a mold.

Permeability of a mold subjected only to radiant heat during casting was found to be approximately the same as the permeability of the same mold tested at room temperature.

Chemical analyses of mold cavity gases showed that hydrogen was present in both dry and green sand molds. However, more hydrogen was present in the green molds. Other gases were CO, CO₂, N₂, O₂ and paraffins.

Introduction

To MAKE GOOD CASTINGS, the physical variables that can affect them must be understood. Both the metal that is poured and the molds into which it is poured have been under constant investigation for many years. However, the effects due to mold cavity gas have been largely neglected.

Sand cores have been heated by various means and the gases evolved have been analyzed¹ or their volumes measured²; gas permeabilities of heated sands have been measured³; and the pressures that develop in sands heated by contact with molten metal have been investigated⁴.⁵. However, these studies have been concerned with pressures within the sand once the mold is filled or with permeability under conditions which are found only in a small protuberance of a sand mold and in small cores.

It was the purpose of this investigation to study the gas within a mold cavity while it was being filled with steel (Grade B—induction furnace melted). This problem was approached from two points of view, the physical and the chemical. From the physical point of view, it was desired to know what gas pressures are developed in a mold cavity, and what correspondence

exists between the permeability of a mold measured by standard procedure at room temperature and that measured during the pouring of a casting. To obtain this information, the following were required:

1. A sand specimen and mold cavity of known di-

2. The standard gas permeability of said specimen.

3. The pressure within the mold at any time during casting as a function of time.

4. The liquid level of metal filling the mold as a function of time.

The volume of gas forced through the sand specimen by rising metal.

From the chemical point of view, the information desired was the analysis of the mold gas in molds of different compositions.

Since different procedures and equipment were used to study these two aspects of mold cavity gas, this work will be presented in two sections: (1) the physical effects of mold cavity gas and (2) the chemical nature of mold cavity gas.

Physical Effects of Mold Cavity Gas

Experimental Equipment and Procedure—Equipment and procedures were designed to give the information outlined in the introduction for molding sands of different permeabilities and composition.

Specimen and Mold Cavity—A 12-in. length of 6-in. pipe was used to contain both the sand specimen and the mold cavity. By ramming sand into the upper 6 in. of the pipe container shown in Fig. 1 and 2, the dimensions of both the sand specimens and mold cavity were determined.

Standard Permeability—An adapter was made by which the permeability of the complete specimen could be measured with a standard permeability machine. A 2-in. by ½-in. reducing bushing was placed over the mercury bath used to seal A.F.S. specimens to the standard tester. A length of ½-in. hose connected this bushing to an 8-in. diameter mercury bath in which the 12-in. high by 6-in. diameter specimen container could be placed. The time for 2000 cc of air to pass through the specimen as well as the prosure forcing it through were measured. In this way the

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^{*} Supervisor Foundry Process Research and ** Assistant Metallurgist, Armour Research Foundation of Illinois Institute of Technology, Chicago.

¹References indicated by superscripts will be found at the end of the report.

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standard permeability of the sand specimen, which was later to be rammed up as a mold, could be measured.

Pressure within the Mold—The hose shown in Fig. 1 and 2 was connected to a tap in the pipe wall 6-in. from the bottom of the pipe. The other end was connected to a water and mercury manometer. The pressure was read at 2-second intervals while the mold was being filled.

The Liquid Level of Metal in the Mold—To determine the liquid level in the mold as a function of time, a series of electrical contacts was placed through the mold wall. In the foreground of Fig. 2 are shown the component parts of a contact assembly. From left to right are a steel contact cemented into a pyrex tube, a rubber sealing bushing and a steel tube holder. Seven of these tubes were welded to the pipe wall at drilled holes which were spaced at 1-in. intervals starting from the bottom. Two tubes were welded at the 6-in. level, one serving as a contact, the other as a pressure tap.

Each of the contacts was connected to one wire from one of six self-starting electric clocks. All the other wires from the clocks were connected to one

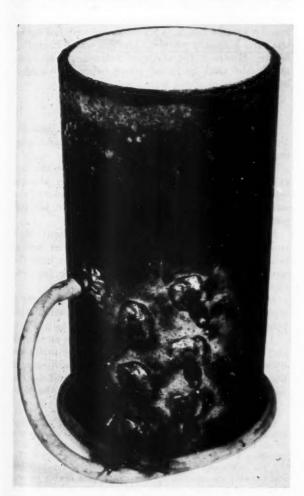


Fig. 1-Pipe specimen container showing electrical contacts and pressure tube.

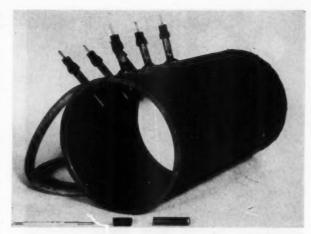


Fig. 2-Pipe specimen container showing detail of electrical contact.

side of a 110-v line. The other side of the line was connected to the steel specimen container. Thus as the metal rose, wetting the contacts, the clocks were started in sequence. By stopping all the clocks at once the height of metal could be read as a function of time. Figure 3 is a diagram of this circuit.

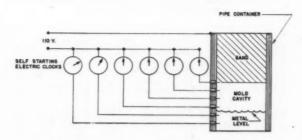


Fig. 3—Circuit for measuring height of metal as a function of time.

Procedure for a Run—The procedure for a run was as follows: A sand specimen 6-in. high was squeezed (by a jolt-squeeze machine) into the 6-in. diameter specimen container, described above. The contacts were inserted into all seven of the tubes to make the mold gastight except for the sand specimen. This complete assembly was connected into a standard permeability tester by means of the mercury seals to measure its permeability. This permeability will be referred to hereafter as room temperature permeability, $P_{\rm RT}$.

After the permeability test had been completed, the contact was removed from one of the uppermost tubes and replaced by the hose leading to the manometers. The entire pipe assembly was next rammed up in a sand containing 4 per cent bentonite and 3 per cent water as a bottom gated mold. This complete mold and measuring apparatus is shown in Fig. 4. Figure 5 gives a sectioned view. When the second hands of the clocks had been set at zero, a run could be made.

Steel was poured into the mold through a dam type pouring cup to control the rate of pour. Pressure read-

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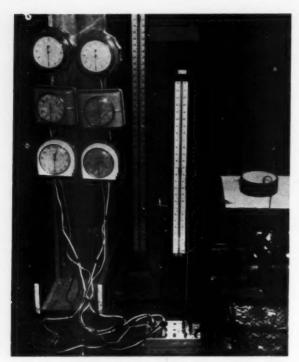


Fig. 4—Complete set up for measuring mold cavity pressures and height of metal.

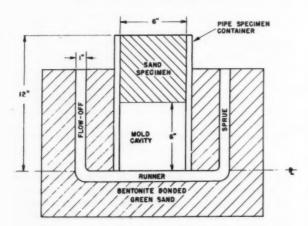


Fig. 5-Sand specimen rammed up as a mold.

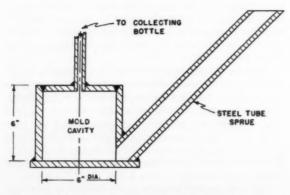


Fig. 6-Welded steel pot mold with steel tube sprue.

ings were made at 2-second intervals after the first clock started to run.

Volume of Gas Forced through the Sand—It was not possible to measure directly the volume of the gas forced through the specimen. Devices for direct measurement would cause a back pressure on the external side of the sand specimen and would result in an erroneous permeability value. Therefore, an additional experiment was required to determine this volume.

A mold cavity equal in dimensions to the mold cavity in the pipe specimen container was made by welding a steel plate cap on a 6-in. length of a 6-in. pipe. A tap was affixed to the top of this cavity. This in turn was connected by rubber tubing to a collecting vessel over a bath of acidified salt solution, used to avoid solution of CO₂. When a casting was poured, gas was displaced from the cavity by the rising metal and trapped in a collection bottle.

When the volumes collected in this way were found to be less* than the volume of the specimen container, a mold was designed with the intention of forcing all the gas in the mold cavity through the outlet at the top. A steel plate was welded to the bottom of one of the 6-in. high capped molds. A 1½-in. steel pipe was welded to this closed chamber to serve as a sprue. This may be seen in Fig. 6. This mold was rammed up in sand and a casting poured. The gas was collected as before. However, the collected volume was approximately the same as found above, where no welded bottom plate or sprue were used.

Discussion

Tabulation—Table 1 lists the significant data for all the molds poured. The following discussion is an interpretation of these data.

Sands Tested—The series of sands tested by the first experimental procedure described ranged in permeability ($P_{\rm RT}$) from 15 to 375. The cereal content of the sands ranged from zero to 7 parts by weight per 100 parts of sand.

Experimental Limitation—The requirement of a contained specimen and mold cavity placed limitations on the amount of sand exposed to heat from the molten metal. As a result, only a cope surface was exposed, and this only to radiant heat.

Mold Pressure and Metal Level—Figure 7 shows a typical pair of curves of mold pressure and metal height as functions of time.

The mold pressure rose from zero to a maximum and dropped off slightly shortly before the mold was filled. This drop-off was not due to pendulation in the manometer. The period of the liquid of the manometer was considerably shorter than the 6 to

* In another attempt to measure the volume of gas forced through the sand specimens, a plate was welded to the top of a 12-in. length of pipe containing a bentonite bonded sand sample.

A tap in the cap was connected to a collection vessel. When this assembly was rammed up as a mold and a casting poured, a volume of gas larger than the volume of the cavity itself was collected. It appeared that this additional volume was due to the expansion of the air in the interstices of the sand once the metal had made contact with the cope.

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TABLE 1-DATA FROM STUDY OF PHYSICAL EFFECTS OF MOLD CAVITY GASES

| Run No. | AFS Permeability of Sands | of Sand Specimens at Room | Permeability of Specimen Calculated from Casting Data | Rate of Metal Rise, | | Corrected Max. Mold Cavity Pressure, In. of Steel | Average Mold* Cavity Pressure, In. of Water | Room Temperature Permeability x Avg. r Mold Pressure | Corrected Avg. Mold Pressure, In. of Water | | Sand+ omposition, Wght., % |
|------------|------------------------------------|---------------------------------|---|------------------------|------|---|---|--|--|------|----------------------------------|
| | PAPS | PRT | Peast | q | Pmax | pm/q x _ | | PRT x A/t | A/qt | | |
| | | | | | | | 7.13 | | | | |
| 2 | 520 | 375 | 282 | 0.67 | 1.08 | 0.227 | 0.82 | 304 | 1.22 | | |
| 3 | 83 | 63 | 31 | 0.44 | 6.5 | 2.08 | 5.1 | 321 | 11.5 | 1.25 | dextrine |
| 4 | 75 | 57 | 32 | 0.44 | 6.0 | 1.88 | 4.9 | 280 | 10.9 | | |
| 5 | 24-27 | 18 | 12.2 | 0.45 | 17.5 | 5.46 | 13.0 | 234 | 28.9 | 0.5 | cereal |
| 6 | 90 | 78 | 59.7 | 0.305 | 2.0 | 0.92 | 1.78 | 139 | 5.8 | | |
| 7 | 84-92 | 51 | 24.4 | 0.295 | 4.5 | 2.18 | 4.2 | 214 | 14.2 | 5 | dextrine |
| 8 | 84-92 | 55 | 72 | 1.14 | 6.7 | 0.81 | 5.6 | 318 | 4.92 | 2 | cereal |
| 9 | 92 | 92 | 67 | 0.65 | 4.4 | 0.95 | 3.35 | 308 | 5.15 | | |
| 0 | 92 | 97 | 67.1 | 0.465 | 3.5 | 1.05 | 2.49 | 242 | 5.35 | | |
| 1 | 92 | 92 | 85 | 0.625 | 3.6 | 0.80 | 2.56 | 245 | 4.1 | | |
| 2 | 18.4-19.6 | 14.7 | 16.7 | 0.65 | 19.5 | 4.20 | 13.6 | 200 | 20.9 | | bentonite |
| 3 | 18.4-19.6 | 14.8 | 20.1 | 0.62 | 17.0 | 3.85 | 10.7 | 158 | 17.3 | | |
| 4 | 18.4-19.6 | 15 | 18.8 | 0.78 | 18.7 | 3.36 | 11.8 | 177 | 15.2 | | bond |
| 5 | 190-194 | 192 | 197 | 0.54 | 1.2 | 0.308 | 0.98 | 188 | 1.81 | | |
| 6 | 190-194 | 197 | 185 | 0.48 | 1.2 | 0.35 | 0.88 | 173 | 1.83 | | |
| 7 | 190-194 | 192 | 175 | 0.475 | 1.3 | 0.378 | 0.94 | 180 | 1.97 | | |
| 8 | 98-104 | 31.5 | 31 | 0.59 | 9.6 | 2.28 | 6.7 | 211 | 11.3 | 5 | dextrine |
| 9 | 98-104 | 30.6 | 28.2 | 0.66 | 11.0 | 2.34 | 8.3 | 254 | 12.6 | 2 | cereal |

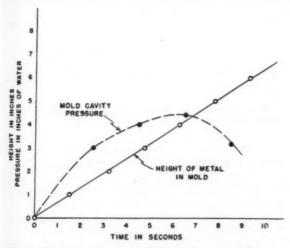
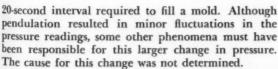


Fig. 7-Typical plots of mold pressure and height of metals as functions of time. Curves taken from run No. 19.



It is interesting to note that the rate of metal rise was virtually constant throughout the interval in which the mold was filled. This appears to indicate that the head of metal in the sprue remained fixed with respect to the metal level in the mold.

Effect of Sand Permeability on Mold Gas Pressure-From the definition of permeability, one would expect, other factors being constant, a hyperbolic relation

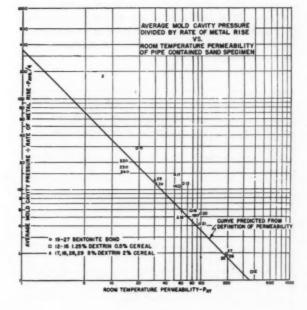


Fig. 8

between permeability and mold pressure. This was borne out by the experiment. Except for molds 16, 17 and 18, an effort was made to keep the pouring rate constant. There was, none-the-less, some variation. Therefore, to eliminate any effect this might have had on pressure, the pressure was corrected to a unit rate of metal rise by dividing by the rate of metal rise before plotting.

In Fig. 8, the average mold pressure divided by the

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rate of metal rise is plotted against the permeability of the specimen measured at room temperature.

The corrected pressure within the mold cavity showed a hyperbolic increase as permeability decreased.

Effect of Sand Composition on Mold Gas Pressure— It was expected that gases evolved from the decomposition of those sands containing cereal binders would increase the mold pressure.

To make any such increase more apparent, log log co-ordinates were used in Fig. 8. Since the ideal curve of P_{avg}/q vs P_{RT} is a straight line on log log paper, any deviations should be more evident here than on rectangular co-ordinates where the curve is a hyperbola.

There was no conclusive evidence that the cereal binders had affected the mold pressure. This lack of variation of pressure with cereal content in this experiment, and gas analyses reported in the next section, indicated that exposure of the cope surface above did not decompose the cereal binders sufficiently to affect the mold pressure.

Effect of Pouring Rate on Mold Gas Pressure—Although the range of pouring rates used was relatively narrow, a plot was made to observe the effect of rate of metal rise on mold pressure. The effect of different permeabilities was eliminated by multiplying mold pressure by room temperature permeability. Figure 9 shows P_{RT} x P_{avg} vs 1/q. The trend was for pressure to increase as rate of metal rise increased. This would be expected from the definition of permeability.

Permeability During Casting—It seemed plausible that the permeability of the specimen as it existed during the pouring of the mold could be measured from the data collected.

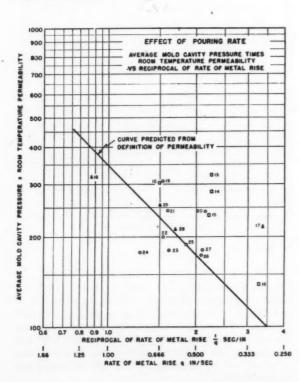


Fig. 9

The familiar definition of permeability is

$$P = \frac{h t}{p a t} \quad Eq. 1$$

where P = permeability number

h = height of specimen in cm

v = volume of gas passing through specimen in cc

p = pressure forcing gas through specimen in gm per sq cm

a = cross-sectional area of specimen in sq cm

t = time to force volume, v, through specimen in min.

However, a more precise statement** was required to describe what took place in the pipe contained mold cavity.

$$P = \frac{h \int_0^t A k q dt}{a \int_0^t p dt}$$
 Eq. 2

ere P = permeability number

h = height of sand specimen in cm

A = area of mold cavity in sq cm

k = a correction factor required because the volume of gas passing through the specimen was not equal to the volume of the mold cavity.

q = rate of metal rise in the mold in cm per sec

a = area of sand specimen in sq cm

p = mold pressure in gm per sq cm t = time to fill mold in min.

** The fundamental definition* of gas permeability of a porous medium is as follows:

$$P = \frac{2 \mu Q_m h}{\gamma_0 a (P_1^2 - P_2^3)}$$

where in terms of the experiment we are considering.

P = permeability

 $\mu = \text{viscosity of mold cavity gas}$

 $Q_m = mass rate of flow of gas$

h = height of sand specimen

γ_• = density of mold cavity gas

a = cross-sectional area of sand specimen

 $p_1 = mold$ cavity pressure $p_2 = atmcspheric$ pressure

The more familiar definition used in testing sands neglects the effects due to viscosity which depend on the analysis of the gas and its temperature. This is permissible, since the test gas is always air and the temperature essentially constant, i.e. room temperature.

Equation 2 also neglects viscosity. However, calculation of the absolute viscosity of a typical mold gas from its composition, showed it to be virtually the same as the viscosity of air.

The following analysis

 $CO_2 = 2.6$ per cent $H_2 = 12.3$ per cent $CH_2 = 41$ per cent

 O_2 — 6.2 per cent CH_4 — 4.1 per cent CO — 16.5 per cent N_2 — 59.3 per cent was obtained from a pipe contained mold which held a speci-

men of the following composition.

No. 60 Sand — 75 parts
Silica flour — 25 "
bentonite — 2.5 "
dextrine — 2 "
cereal — 1 "
water — 3 "

Fortunately as shown in Fig. 7 rate of metal rise, q, which is the slope of the height of the metal vs time $\frac{H}{t}$ curve was a constant, $\frac{H}{t}$, where H is the height of the mold cavity and t the time to fill the mold. Furthermore, because the pipe contained the mold cavity as well as the sand specimen, A = a. Hence

$$P = \frac{h k \frac{H}{t} \int_{0}^{t} dt}{\int_{0}^{t} p dt} = \frac{h k H}{\int_{0}^{t} p dt} Eq. 3$$

The denominator is then the area under the curve of mold pressure vs time.

This expression for permeability was first applied to the data directly without the correction factor, k. In other words, it was assumed that the volume of gas passing through the specimen was the same as the volume of the mold cavity itself. The permeabilities so calculated were roughly twice as large as the room temperature permeabilities. Since volume was the only variable that had not been measured directly, it was then investigated. The experimental method used has been previously described.

The results of these measurements are shown in

TABLE 2

| | 5 | pecim | en — | | Cavity Volume | Collected Volume | Ratio |
|-----|-------|---------------------|---------|--------|------------------|---------------------|--------|
| Cap | | 6-in. sa over ca | | ecimen | 2770 | 3540 | 0.78 |
| (1) | Cap | directly | over | cavity | 3100 | 1080 | 2.85 |
| (2) | 27 | | 22 | 89 | 2650 | 1160 | 2.30 |
| (3) | 99 | 9.0 | | 9.9 | 2650 | 920 | 2.87 |
| (4) | Steel | pot | | | 2640 | 1097 | 2.41 |
| (5) | 9.9 | *** | | | 2640 | 1005 | 2.63 |
| 1-1 | | | | | | Avg. ratio | = 2.62 |
| | | | | k = | 1/2.62 | | |

An explanation for this discrepancy between cavity and collected volume was found while pouring the steel pot mold. When this mold, with both mold and gating system completely enclosed in steel, was poured, gas was audibly ejected through the sprue with sufficient force to carry a small amount of metal with it. This occurred just after pouring had begun. After this initial escape of mold gas, the mold filled quietly without forcing gas or metal back through the sprue. Evidently the heat from the molten steel caused the gas in the mold cavity to expand rapidly and escape through the gating system before that exit was completely closed off by the rising metal.

Figure 10 shows the casting permeability, Peast,

plotted against P_{RT} . P_{cast} was calculated by applying the correction, k, to the data collected during the pouring of the casting. For the most part, the casting permeability is in close agreement with the room temperature permeability.

It should be emphasized here that P_{RT} means the actual permeability of the sand specimen not that of an A.F.S. test piece. Figure 11 shows the relationship between P_{RT} and P_{AFB} for the sand tested. For the flowable straight bentonite sands both permeabilities were the same. For those sands less flowable as shown by those containing cereal binders, the P_{AFS} was much higher than the corresponding P_{RT} .

Conclusions

1. Mold Pressures—Mold cavity pressures that develop with small castings during pouring are small in terms of their effect on filling of mold. It can be seen from Fig. 12, where maximum pressure in inches of steel is plotted against permeability, that only a small head is required to overcome pressure in the mold cavity.

2. Factors Affecting Pressure—Several factors operate to keep the mold cavity pressure low. In the first place, a considerable volume of gas escapes through the gating system. With less gas to force through the mold the resultant pressure is lower. Furthermore, in an ordinary mold the side walls and open risers furnish additional paths of escape for gas not found in

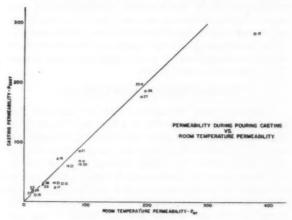


Fig. 10

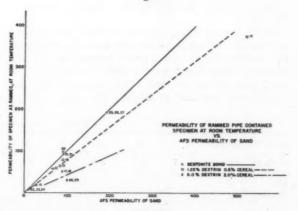


Fig. 11

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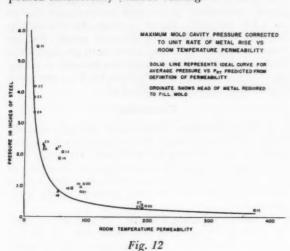
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The absolute viscosity of this gas at 60 F and 30 in. Hg was calculated to be 0.000171. The viscosity of air under the same conditions is 0.000178.

As for temperature, it is well known⁸ that it is a matter of minutes before the sand more than ½-in. back from the sand metal interface rises much above room temperature. Since, at most, only 20 sec was required to fill a mold in these experiments, it is reasonable to assume that the temperature of the bulk of the sand specimen was room temperature during that interval in which permeability was being measured.

the pipe contained mold. Hence, even lower pressures would be expected in ordinary molds.

3. Application—These conclusions of low mold pressures are in agreement with the findings of R. Adams, who showed that small molds could be poured satisfactorily without venting.



4. Standard Permeability as a Measure of Casting Permeability—In regard to permeability, which gives an indication of the mold pressures which may be anticipated, room temperature permeability of the actual mold and not the standard A.F.S. permeability is the valid measure of permeability during casting. It is possible that this statement can be extended to

to fill than those studied here.

Chemical Nature of Mold Cavity Gas

include castings that are even larger and take longer

Equipment and Procedure—Gas for analysis was withdrawn from the cavity of a 6-in. by 6-in. by 8-in. high sand mold via a glass tube inserted through the cope of the mold. This tube was connected to the top of a 250-cc bottle by rubber tubing. On the side of the bottle near the bottom was a nipple. One end of a length of tubing was attached to this nipple; the other led to an open jar. When the bottle was filled with mercury and the jar partially filled, it was possible to adjust the level of the mercury by the relative position of the jar and bottle. This is shown in Fig. 13.

At the start of a run, the mercury level was adjusted so that the collection bottle was full. To withdraw the sample, the jar was rapidly lowered so that the mercury might flow from the bottle to the jar. The head between the jar and bottle was such that the sample was collected in slightly less time than re-

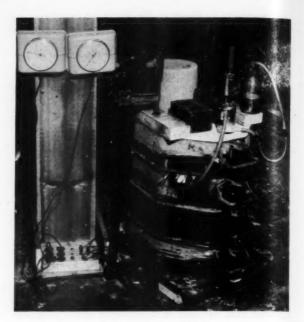


Fig. 13-Set up for collecting mold cavity gas.

quired to fill the mold with steel. The size of the sample and the rate at which it was withdrawn were such that the pressure in the mold was always positive. In this way, no air was drawn into the mold cavity through the mold wall.

Electrical contacts were placed at the bottom and I in. from the top in the mold. When metal reached the contacts, the circuit was completed to two electric clocks. When the first clock started, the leveling bottle was lowered. When the second clock started, the inlet hose to the collection bottle was pinched off. In this way, the sample was withdrawn during the filling of the mold.

Outline of the Experiment—A set of 12 castings was poured in molds of the same dimensions, 6 in. by 6 in. by 8 in. high. Nine of the molds were homogenous, each entire mold being made from a different sand mixture. Three molds were composite, part being made of a dried oil sand and part from a dry sand that contained no organic binders. Two sections of each composite mold were made from the sand without organic binders, and the third section, drag, cope and cheek was, in turn, oil sand.

The relationship between the various analyses showing the effect of moisture, cereals, and oil, will be discussed later. Table 3 shows how the water and cereal content of the molds used were varied. Table 4 shows how the analyses were compared to show the effect of the different variables on the gas analyses. Table 5

TABLE 3-STRUCTURE OF EXPERIMENT

| Mold Designation | A | В | C | D | E | F | G | Н | I | J | K | L |
|----------------------------|-----|-----|-----|---|-----|---|---|---|-------|-------|-------|---|
| H ₂ O, per cent | 0 | 2.5 | 5.0 | 0 | 3.4 | | 4 | 0 | | | | |
| Cereal, per cent | . 0 | 0 | 0 | 1 | 1 | | 4 | 4 | | | | |
| Oil | | | | | | x | | | Drag | Cheek | Cope | |
| Bentonite | | | | | | | | | Cheek | Cope | Cheek | |
| | | | | | | | | | Drag | Drag | Drag | |
| Steel | | | | | | | | | | | | x |

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TABLE A

| Effect of | Molds | Figure No | |
|--------------|--------------|-----------|--|
| Cereal | A D H | 14 | |
| Water | A B C | 15 | |
| Water on | | | |
| Cereal | D & E; H & G | 16 | |
| Cereal on | | | |
| Water | B & E; C & G | 17 | |
| Dry Sand | | | |
| Steel | A & L | 18 | |
| Oil Position | FIJK & A | 19 | |
| Commercial | | | |
| Sands | AE & F | 20 | |

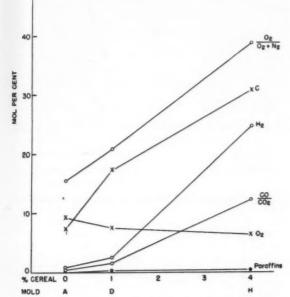


Fig. 14-Effect of cereal binder on mold gas.

contains the sand mixes and gas analyses for all the molds compared in Table 4.

Effect of Varying Mold Compositions on Mold Gas Analyses—Presentation of Data—A number of graphs have been prepared to show the effect of the different mold mixtures on the mold gas. The 12 analyses have been grouped in several different ways to show the effect of single variables. The principal changes in the molds were moisture and organic binder content.

The analyses showed that the gases present were CO, O₂, CO₂, H₂, paraffins and N₂. Except for the paraffins these gases were found in every analysis.

Because of the small size of the gas sample, it was found to be impractical to analyze for water vapor.

In presenting the data graphically, free O_2 , paraffins, and H_2 have been plotted as per cent of gas collected. CO_2 and CO have been given as the ratio of CO to CO_2 , and carbon as the sum of CO_2 and CO. In addition, total oxygen has been compared to total oxygen plus nitrogen as follows:

Total $O_2 = CO_2 + O_2 + \frac{1}{2}CO$ % O_2 of $O_2 + N_2 = \text{total } O_2/\text{total } O_2 + N_2$

The reason for plotting these particular variables— $O_2/(O_2 + N_2)$, CO/CO_2 , and C—was that they gave information not shown in the simple analysis. The ratio of $O_2/(O_2 + N_2)$ shows whether more or less oxygen is present than was found in the air initially in the mold. If this ratio is higher than 0.21, it is assumed that oxygen has come from sources other than air. These sources are water, oil or cereal. Rather than plot CO and CO_2 individually, their sum is plotted showing how the total amount of carbon in the mold atmosphere changes. However, the relationship between CO and CO_2 , given by the ratio CO/CO_2 is plotted.

Effect of Cereal Binder-Figure 14 shows the effect on the mold gas of increasing amounts of cereal binder

TABLE 5-GAS ANALYSES

| | A | В | С | D 4% Bento- | E 4% Bento- | F 1.5% Cereal |
|--------------------|--------------|-----------------------|-----------|----------------|-----------------------|------------------|
| | 4% | 4% | 4% | nite, 1% | nite, 1% | Core Oil 1.0% |
| Sand | Bentonite | Bentonite | Bentonite | Cereal | Cereal | Kerosene 1.0% |
| Composition | Oven Dried | 2.5% H ₂ O | 5% Water | Dried | 3.4% H ₂ O | Dried |
| CO ₂ | 4.9 | 3.3 | 2.0 | 6.5 | 2.8 | 5.0 |
| O ₂ | 9.2 | 6.2 | 2.9 | 7.4 | 1.7 | 5.2 |
| CO | 2.4 | 6.3 | 11.3 | 10.8 | 11.5 | 30.4 |
| H ₂ | 0.9 | 33.0 | 46.1 | 2.5 | 50.3 | 25.6 |
| Paraffins | 0 | 1.2 | 0 | 0.4 | 2.9 | 2.2 |
| N ₂ | 82.6 | 49.7 | 37.7 | 72.4 | 30.8 | 31.6 |
| %O2 of O2+N2 | 15.7 | 20.2 | 21.7 | 21.0 | 25.0 | 44.5 |
| CO/CO ₂ | 0.49 | 1.91 | 5.7 | 1.66 | 4.10 | 6.08 |
| %C | 7.3 | 9.6 | 13.3 | 17.3 | 14.3 | 35.4 |
| | G | Н | I | J | K | L |
| | 4% Cereal | 4% Cereal | | | | Steel |
| Sand | 4% Bentonite | 4% Bentonite | Oil | Oil | Oil | . Cavity |
| Composition | 4% Water | Dry | Drag | Cheek | Cope | & Sprue |
| CO ₃ | 2.5 | 2.3 | 6.4 | 6.4 | 6.8 | 5.0 |
| O ₂ | 3.0 | 6.2 | 4.3 | 5.5 | 8.9 | 9.4 |
| CO | 30.5 | 28.7 | 7.9 | 11.1 | 2.5 | 4.1 |
| H ₂ | 46.0 | 24.8 | 2.6 | 7.5 | 0.6 | 0.5 |
| Paraffins | 4.6 | 0.6 | 0.1 | 0 | 0 | 0.2 |
| N ₂ | 13.2 | 37.4 | 78.7 | 69.5 | 81.2 | 80.8 |
| %O2 of O3+N2 | 63.0 | 39.0 | 15.7 | 17.4 | 17.2 | 16.9 |
| CO/CO, | 12.2 | 12.5 | 1.23 | 1.73 | .37 | 0.82 |
| %C | 33.0 | 31.0 | 14.3 | 17.5 | 9.3 | 9.1 |

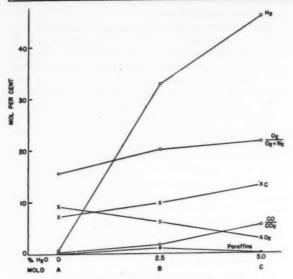


Fig. 15-Effect of mold moisture on mold gas.

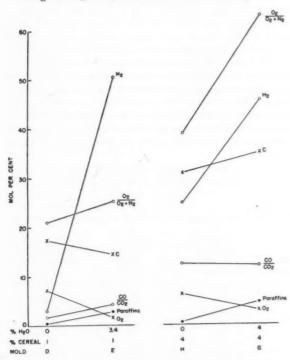
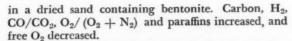


Fig. 16-Effect of moisture on mold gas in presence of cereal binder.



Evidently the cereal binder supplied carbon, hydrogen and oxygen to the mold gas. Even though the oxygen present was considerably in excess of that which would balance the nitrogen at the normal air ratio of 1 to 4, most of it was tied up by the carbon. As a result, there was less free oxygen in the mold "D" which contained 1 per cent cereal and mold "H" which contained 4 per cent cereal than in mold "A"

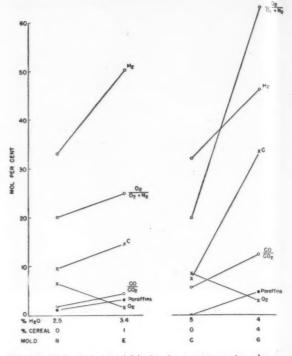


Fig. 17-Effect of cereal binder in presence of moisture.

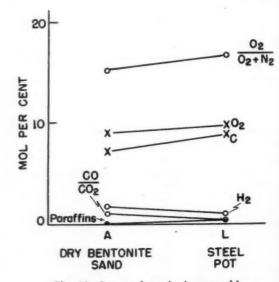


Fig. 18-Gas analyses in inert mold.

which contained no cereal binders.

Effect of Moisture—Figure 15 shows the effect of water on the mold gas in a bentonite bonded sand. The most pronounced effect of the initial addition of 2.5 per cent was to raise the hydrogen content of the mold gas to a high level. Carbon, CO/CO_2 , $O_2/(O_2 + N_2)$ and paraffins were also raised, but to a lesser degree. The free oxygen decreased. These effects of water were in the same direction as the effects of cereal. However, the cereal additions raised C and $O_2/(O_2 + N_2)$ to a greater extent than water addi-

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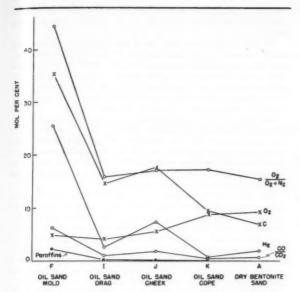


Fig. 19-Effect of position in mold on decomposition of oil sand.

tions, and water additions raised the hydrogen content more than cereal additions.

Effect of Water In Presence of Cereal—In Fig. 16, the effect of additions of water to a given level of cereal is shown. Here are two contradictions to the general increase of C and CO/CO₂ with an increase in water or cereal. When the moisture of the 1 per cent cereal mix was increased from 0 to 3.4, the C decreased from 17.3 to 14.3. When the moisture of the 4 per cent cereal mix was increased from 0 to 4, the CO/CO₂ ratio decreased from 12.5 to 12.2. The trend of the analyses, except for those two contradictions, was the same as observed with increasing water in Fig. 15.

Effect of Gereal in Presence of Water—The result of adding cereal in the presence of water is shown in Fig. 17. Here, as before, an increase in cereal increases all constituents except the free O_2 .

Inert Molds—In the interest of finding what the mold gas would be in an inert mold, a casting was poured in a mold that was made from welded steel with a welded steel sprue. It can be seen in Fig. 18 that the differences in the gas from the dried bentonite bonded mold "A" and the steel pot "L" were insignificant. Evidently both molds were essentially inert. The gases collected were the result of interaction between metal and air rather than metal, mold and air.

Oil Sands—A series of molds was made in which one section was an oil sand and the balance of the mold a dried bentonite bonded sand. The pattern was a 6-in. by 8-in. high block. The mold was parted at the top and bottom of the block. First, only the drag was oil sand; next, only the cheek, and then only the cope. Finally a complete mold was made from oil sand. The results are shown in Fig. 19.

One might expect the sum of C or H_2 for the three complete molds to be equal to the C or H_2 in the mold that was entirely oil sand. Although this is roughly true for C, it was not true for H_2 .

Comparison of the analyses shows that the oil cheek

mold produced a gas higher in C and H₂ than either the oil cope or oil drag molds.

Commercial Mixes—A comparison of the mold gas from mold mixtures that might be found in commercial practices with a dried bentonite bonded sand is shown in Fig. 20. "A" is the dried bentonite mix, "E" a green sand, and "F" an oil sand. The highest hydrogen levels were obtained in the green sand mold.

Summary

From the results of these tests, the following generalization can be made: Within the limits of the additions made, any addition of organic binder or water causes an increase in each of the variables $O_2/\left(O_2 + N_2\right)$, C, H_2 , CO/CO_2 and a decrease in free O_2 .

Conclusions

In studying the chemical nature of mold cavity gases, the analyses of the gases from several molds were examined. No tests were performed to determine the possible effects of these gases on the steel. It is therefore difficult to do more than report the gases present.

However, some conjectures may be made. It has been reported that castings poured in green sand molds are more apt to have porosity than castings poured in dry sand molds. The adverse effect of green sands was attributed to atomic hydrogen from decomposition of water in the sand. Now it was shown above that hydrogen levels for dry sand molds could be as high as 50 per cent of the hydrogen levels in green sand molds.

There are two possible explanations for this lesser

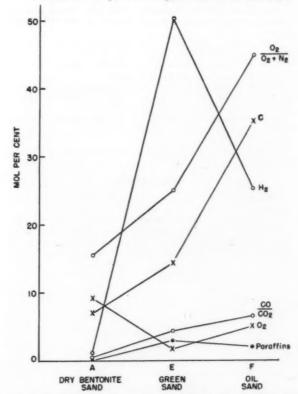


Fig. 20—Comparison of gas analyses from inert mold and typical green and dry sand molds.

effect of dry molds.

- 1. The hydrogen level is lower than green sand
- 2. Cereal and oil binders decompose directly to H₂ without the formations of atomic hydrogen.

Contributing Personnel

Contributions to the rationalization of the results of the experiment on mold cavity pressures were made by Mr. K. W. Miller and Mr. O. E. Teichman.

Acknowledgment

The authors wish to express appreciation to Armour Research Foundation of Illinois Institute of Technology for permission to conduct this work and for the financial assistance secured from the American Steel Foundries Research Fund established at the Foundation for foundry investigations.

The interest and discussion by Mr. G. A. Lillieqvist and Mr. J. Rassenfoss of the American Steel Foundries was happily received as encouragement and stimulation.

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DISCUSSION

- Chairman: C. W. BRIGGS, Steel Founders' Society of America. Cleveland.
- Co-Chairman: R. E. KERR, Pettibone-Mulliken Corp., Chicago. JOHN McBroom:1 I was interested in the authors' observation about the increase in hydrogen pressure or hydrogen volume in greensand molds as compared to drysand molds or other molds.

- Does the author have information as to the effect of this increased percentage of hydrogen to the surface tension of the
- We have poured the same metal into greensand and drysand molds alternately. We could run the drysand molds consistently and often misrun the greensand molds. What is the possible effect of the hydrogen on surface tension of the metal? Was our lack of fluidity in the greensand molds due to the chilling effect of evaporating the moisture on the mold surface or the increase of surface tension due to hydrogen formed?
- Mr. Locke: I have no information or data on the change in properties, particularly, surface tension due to hydrogen. I was apprehensive that any mention of hydrogen would bring about such a question because of the way many investigators have discussed hydrogen with regard to its effect on steel. I do not know what the effects of the change in the analysis of the mold cavity gases are on the castings.
- We could find no evidence from our particular test blocks to substantiate conclusions concerning the casting qualities. I do not believe that the gas analysis as we obtained it permits any theorizing about reactions between the gases and molten metal.
- As far as your inability to run a casting without cold shuts when using green sand, I think the difficulty will be found in the loss of temperature from the surface of the metal. It is possible that you have a greater chilling effect in the greensand molds than you did have in drysand molds.
- The high humidity atmosphere in a greensand mold can also cause a loss of heat from the metal.
- R. E. Morey: When C. W. Briggs was with the Naval Research Laboratories, he and I ran some experiments in measuring the volume of gas given off by a sand specimen where metal was cast against the specimen. We did not try to draw off the gas from the mold cavity but we measured the gas coming off from the back side of the specimen. We were amazed at the volume of gas which came off which was many times the volume of the specimen.

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- MR. LOCKE: I remember the experiments you refer to. I believe this work is referred to in the bibliography in this paper. It was the gas that was coming off on the mold side rather than in the cavity. The fact that you could check that much volume on the outside is no reason that you will have that much volume on the inside of the mold. There is certainly a large temperature differential between the metal-sand surface and the outside of the mold. This would cause a correspondingly large pressure differential and thus move the gases outward.
- C. E. Sims: In the case where water content breaks down to give a high proportion of hydrogen, did you notice any difference that can be attributed to hydrogen?
 - It has a lower density than oxygen or CO2.
- MR. LOCKE: No, we did not notice this difference as far as permeability is concerned. We did attempt to rationalize in the paper any effect of a change not only in diffusibility and in viscosity of the gas due to changes in chemical makeup and temperature, but also changes in density.
- Of course, when we rationalize we do everything possible to make our thoughts agree with the nice picture we show by the

¹ Partner, Stainless Foundry & Engineering Co., Milwaukee.

³ Metallurgist, U. S. Naval Research Laboratory, Washington, D. C.

⁸ Assistant Director, Battelle Memorial Institute, Columbus, Ohio.

AN INVESTIGATION OF METAL PENETRATION IN STEEL SAND CORES

By

S. L. Gertsman* and A. E. Murton**

ABSTRACT

Effect of ferrostatic pressure on metal penetration of steel sand cores is shown. The metal head of the standard casting was varied and the relative importance of different sand variables was studied. It was found that cores made with 100 per cent silica flour resisted penetration under the highest ferrostatic pressure used in the experiments. In castings with a high metal head, the average fineness to prevent penetration should be less than 0.075 mm for a subangular sand. No significant difference in penetration of wide and narrow range sands was observed when the average screen size was the same. Effects of ramming, pouring temperature, core temperatures, silica flour wash, and wood flour were studied and the results are discussed.

Introduction

Penetration of steel into sand cores is one of the most troublesome conditions encountered in the foundry. It is sometimes more expensive to carry out the cleaning operation on a casting which has been severely penetrated than to produce the casting. Consequently, it is not unusual to find castings scrapped due to metal penetration. In a survey of Canadian steel foundries made several years ago in order to ascertain the most pressing technical difficulties facing the industry, this problem was placed at the top of the list. Accordingly, the Bureau of Mines undertook an investigation of metal penetration.

This paper outlines some of the work done on this project. Induction melted mild steel of a commercial grade was used for all of the tests. As a preliminary measure, a cylindrical casting (Fig. 1) was designed as a standard. It weighed 45 lb, was $5\frac{1}{2}$ in. in diameter at the bottom, 7 in. across the top, and 5 in. high. This casting held four rammed cores (3 rams) $1\frac{1}{8}$ in. in diameter by 2 in. in height. These cores projected $1\frac{1}{2}$ in. into the bottom of the casting.

After a number of heats of steel were poured using the 45-lb casting an unusual phenomenon was observed. When the induction melted metal was fully deoxidized with silicon or aluminum there was no penetration of test cores made from a normal core mixture containing 1 per cent cereal and 1 per cent linseed-base core oil, baked for 2 hr at 400 F (204 C). This was the case even when excessively high pouring temperatures up to 3170 F (1743 C) were employed. Sands of various grain size were used in test cores. Even the No. 30 A.F.S. Ottawa sand, which had the largest voids, and hence of the sands tested might be considered to be the most susceptible to metal penetration, did not penetrate under these conditions with this small casting. When insufficient silicon was used in deoxidizing the steel, however, so that gassy metal resulted, test cores were penetrated severely. Figure 2 shows the effects for both gassy and fully-killed steel. Penetration is shown in the casting at the left whereas the casting at the right shows no penetration. The same cores were used on both castings and all condi-



Fig. 1-Standard casting, 5 in.

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^{*} Physical Metallurgist, ** Metallurgist, Physical Metallurgy Division, Bureau of Mines, Ottawa.

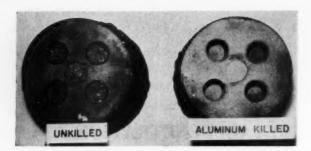


Fig. 2-Effect of deoxidation practice on metal penetration.

tions other than gas content of the metal were constant.

Although a number of tests (Ref. 1, 2) on various types of cores were carried out with underdeoxidized metal, it was apparent from the results that the conditions which prevailed in producing the 5-in. casting were not severe enough to cause penetration under proper melting procedures. The use of wild metal to produce metal penetration, was so far from normal that the results of tests would be suspect as not necessarily applying in actual plant practice. To determine if greater ferrostatic pressure would overcome this difficulty, the height of the casting was increased. The same gating, core arrangement, and casting diameter were retained. Casting heights of 5, 17 and 30 in. were used. Test cores were made from Series A (See Appendices). The side and bottom views of these castings





Fig. 3-Effect of ferrostatic pressure. Side and bottom views. Core Series A.

are shown in Fig. 3. The results from these tests, listed in Table 1, indicate that metal penetration can be produced with normal foundry cores and fully-killed steel by increasing the metal head. Core No. 4A consists of a serpentine rather than a silica sand base. (See Appendix I, Note A). Pouring temperatures were measured with a platinum-platinum 13 per cent rhodium thermocouple.

Table 1*-Effect of Casting Height Core Series A-2950 F (1620 C)

| Core No. | | 5-in. | 17-in. | 30-in. |
|----------|-----------------|-------------------------|---|--------------------------------------|
| 1A | No p | enetration | No penetration | Heavy veining, slight penetration |
| 2A | 27 | ** | Completely penetrated | Completely penetrated |
| 3A | ** | ** | No penetration | * " |
| 4A | Pene | trated | Completely penetrated | ** |
| 1 | einin Burn-c | g—a fin p n—less tha | owed in this pap rojecting into the an $\frac{1}{16}$ in. penetr ything over $\frac{1}{16}$ in | e core cavity ation |

Effect of Ramming

In the above tests the cores were rammed with three rams of the Dietert sand rammer. The effect of ramming was studied by varying the number of rams. The results of these tests on the 30-in. casting, using core Series A poured at 2800 F (1538 C) and 2950 F (1620 C), are shown in Table 2 and Table 3 (Fig. 4). Increased ramming had no effect on cores 2A and 3A as all of these cores were penetrated completely. For core No. 1A the major effect of varying the number of rams was to influence the degree of veining. At both temperature levels medium ramming produced minimum veining.

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Effect of Pouring Temperature

A series of heats was poured to determine the influence of pouring temperature on metal penetration. Thirty-inch castings were poured at 2800 F (1538 C), 2950 F (1620 C) and 3080 F (1693 C) using cores of Series B (variation in silica flour content from 0 to

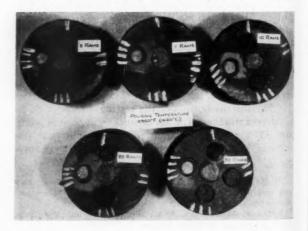


Fig. 4-Effect of ramming. Core Series A.

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TABLE 2-EFFECT OF RAMMING-2800 F 30-in. Casting, Core Series A

| Core No. | 3 Ra | ims | 5 Rams | 7 Rams | 10 Rams | 20 Rams | 50 Rams |
|------------|------------|--------------------------|-----------------------|-----------------------|---------------------------------|--------------------------|-----------------------|
| 1A | | netration avy veining | No penetration | No penetration | Slight burn-on at the bottom | Veining at the bottom | Heavy veining |
| 2 A | Completely | y penetrated | Completely penetrated | Completely penetrated | Completely penetrated | Completely penetrated | Completely penetrated |
| 3A | | ** | ** | ** | ** | ** | |
| 4A | ** | 29 | Penetrated 3/4 way | Penetrated 1/2 way | Penetrated 1/2 way | Penetrated | ** |

TABLE 3-EFFECT OF RAMMING-2950 F 30-in, Casting, Core Series A

| Core No. | 3 Rams* | 5 Rams | 7 Rams | 10 Rams | 20 Rams | 50 Rams | |
|----------|--------------------------------------|---|--|-----------------------|---------------------------------------|--------------------------------------|--|
| 1A | Slight penetration, heavy veining | Very slight vein- ing, no pene- tration | Very slight vein- ing, very little penetration | Slight burn-on | Slight burn- on, slight veining | Slight penetration and veining | |
| 2A | Completely penetrated | Completely penetrated | Completely penetrated | Completely penetrated | Completely penetrated | Completely penetrated | |
| 3A | 98 | ** | ** | 99 | ** | 99 | |
| 4A | Penetrated 1/8 way | Penetrated 1/2 way | Penetrated 1/3 way | Penetrated 1/3 way | Penetrated 1/4 way | Penetrated 1/8 way | |
| | * See Fig. 3 | | | | | | |

100 per cent, Appendices). A slight increase in penetration is observed as the temperature is increased to 3080 F.

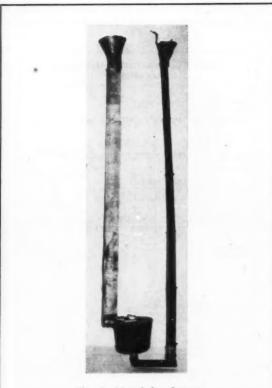


Fig. 5-Metal head test.

TABLE 4—EFFECT OF POURING TEMPERATURE 30-in. Casting, Series B

| Core No. | 2800 F | 2950 F | 3080 F |
|----------|-------------------------|-------------------------|-------------|
| 1B | Penetrated | Penetrated | Penetrated |
| | 7/8 way | 7/8 way | 7/8 way |
| 2B | Bad burn-on | Bad burn-on | Penetrated |
| 3B | Slight burn-on, veining | Slight burn-on, veining | Bad burn-on |
| 4B | No penetration | No penetration | Burn-on |

Comparative Effects of Ferrostatic Pressure and Core Temperatures

Tests were carried out to determine whether ferrostatic pressure was the major factor operating when metal penetration was obtained by increasing the metal head. Cores of Series B (see Appendices) were used in these experiments.

Effect of Large Volume of Metal Passing the Cores (Bleed-Off Test)—When the metal head was increased a larger volume of metal passed by the cores. This could be expected to increase the temperature of these cores. A test was made to determine whether this effect by itself could produce penetration. In this test, 450 lb of metal were poured into a 5-in. high casting. The excess metal was bled off at the top of the casting. No penetration occurred with any of the cores. Temperatures of the cores were recorded and are discussed below.

Effect of Increased Pressure Alone (Metal Head Test)—A casting (Fig. 5) was designed to evaluate the effect of pressure by itself. This consisted of the standard 5-in. high casting with an offset riser, 2½ in. in diameter and 45 in. high. Metal was poured into the offset riser as soon as the 5-in. casting was filled. Table 5 and Fig. 6 show that metal penetration was obtained

TABLE 5-EFFECT OF METAL HEAD AND TEMPERATURE

| | , | | | Core Ser | ies B | | | | |
|----------------|---|-----------------------|--------------------------------|-------------------|-------------------|---------------------|-------------------------------|---------------------------|--------------------------|
| | Bleed-Off Test (5-in. Casting 450 lb metal) | (5-in. | Head Test Casting, head) | 5-in. | Casting | 30-in. | Casting | 50-in. | Casting |
| Tem Core No | p., F 2950 | 2800 | 2950 | 2800 | 2950 | 2800 | 2950 | 2800 | 2950 |
| 18 | No penetration | Penetrated 3/4 way | Penetrated 7/8 way | No Penetration | No Penetration | Penetrated 7/8 way | Penetrated 7/8 way | Penetrated 7/8 way | Completely Penetrated |
| 2B | 92 | Burn-on | 3/16 in. at inside corner | ** | . " | Bad-burn- on | Bad-burn- on | Penetrated 1/2 way | Penetrated 3/4 way |
| 3B | * | Slight burn-on | No penetration | ** | ** | Veining, burn-on | Veining, slight burn-on | 1/8 in. on inside-wall | 3/8 in. at inside wall |
| 4B | ** | No Penetration | ** | *** | ** | No penetration | No penetration | No penetration | No penetration |

on No. 1B core poured at 2800 F and on No. 1B and 2B cores at 2950 F. Temperatures of the cores (poured at 2950 F) used in this test were also recorded and are discussed below.

Effect of Variation of Metal Head on Core Temperatures—Castings, with heights of 5, 30, and 50 in., were poured at 2800 F (1538 C) and 2950 F (1620 C). Core temperatures of the castings which were poured at 2950 F were measured with platinum-platinum 13 per cent rhodium thermocouples protected by 0.25-in. O.D. quartz tubing having a wall thickness of 0.025 in. A hole was carefully drilled in the center of each core to just within ½-in. of the top. The quartz tube containing the thermocouple wires was then inserted into this hole. The thermocouples were connected

through a manually operated multiple switch to an automatic temperature. The set-up is shown in Fig. 7. A time interval of 5 sec for each core was used in operating the multiple switch. Thus, for each core the instrument would record for 5 sec, lapse for 15, again record for 5 sec and so on. The resulting interrupted curves obtained on the automatic temperature recorder chart for each of the four cores were then interpolated into smooth curves. These are shown in Fig. 8, 9, and 10, each of which is the result of several tests. The limit of error between tests was approximately \pm 30 F. The cores, however, always had the same relative order of temperatures.

In comparing the curves of the core temperatures for the castings of the three different heights the fol-

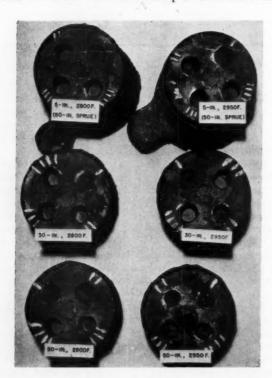
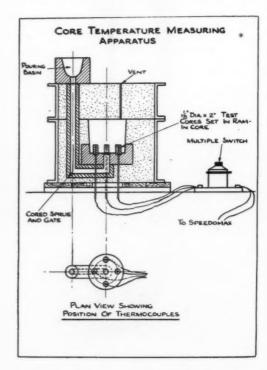


Fig. 6-Effect of metal head and temperature.



TEMPERATURE - FAHR

Fig. 7-Core temperature measuring apparatus.

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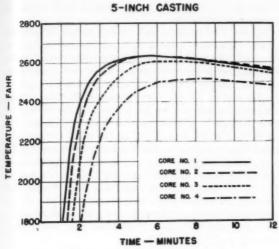


Fig. 8-Heating curves for cores, Series B.

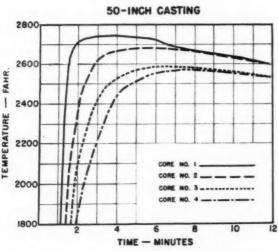


Fig. 10-Heating curves for cores, Series B.

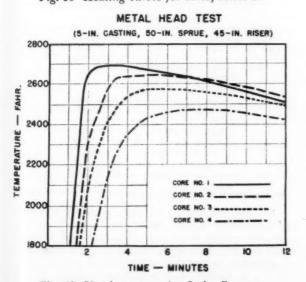


Fig. 12-Heating curves for Series B cores.

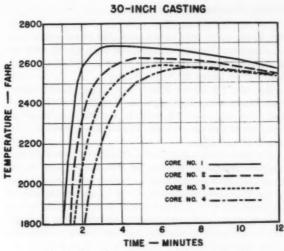


Fig. 9-Heating curves for cores, Series B.

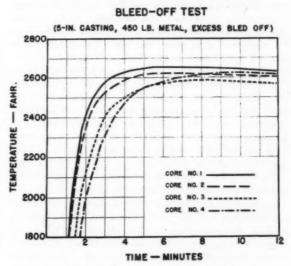


Fig. 11-Heating curves for Series B cores.

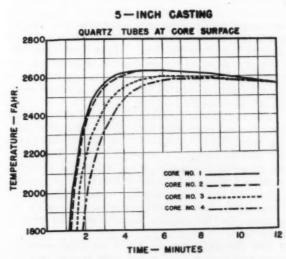


Fig. 13-Heating curves for core Series B.

lowing observations can be made:

(a) No. 1B Cores—The 5-in. curve was the coldest, the 30-in. curve was hotter and the 50-in. curve was the hottest. The No. 1B cores for both the 30 and 50-in. castings were penetrated. These cores had no silica flour. (See Series B, Appendices.)

(b) No. 2B Cores—There was no significant difference between the curves of the No. 2B cores in the 5 and 30-in. castings. The core in the 50-in. casting was hotter and was penetrated. Burn-on was observed for

the 30-in. casting.

(c) No. 3B Cores—No significant difference in the temperature curves for the No. 3B cores in any of the three castings was observed. There was no penetration to the thermocouple position (1/8 in. below the top of the center of the core) for any of the cores. There was some burn-on and some veining for the 30-in. casting. There was some penetration at the inside wall of the 50-in. core cavity.

(d) No. 4B Cores—The temperature curve of the No. 4B core was cooler for the 5-in. casting than were those of the 30-in. or 50-in. castings. There was no significant difference between the curves for this core for the 30-in. and 50-in. castings. The No. 4B core

was not penetrated in any of the castings.

Further observations can be made from the curves. Penetration appears to be accompanied by increased core temperatures. These (1/8 in. below the core surface) are below the pouring temperature of the steel. Increased silica flour additions result in lower core temperatures and slower heating-up cycles of the cores. The No. 4B core (100 per cent silica flour) was not penetrated in any of the castings, thus it can be used to estimate the severity of the temperature conditions to which the cores were subjected.

Figures 11 and 12 show the core heating curves for the Bleed-Off and the Metal Head tests previously described. In comparing the No. 4B cores in these two charts, it is apparent that the temperature conditions in the Bleed-Off test were more severe. None of the cores in this casting was penetrated, whereas there was complete penetration of the No. 1B core in the Metal Head test. From a comparison of the temperature curves of these two No. 1B cores, it appears that the temperature conditions to which this core was subjected in the Metal Head test were much more severe than in the Bleed-Off Test. These conditions were evidently caused by the penetration of metal into the core. Thus it appears that in this case, penetration is the cause rather than the effect of the increased severity of temperature conditions.

The 5-in. casting with the 50-in. head (Metal Head test) was not penetrated as severely as the 50-in. casting (see Table 5). This could be due possibly to the fact that as a result of the smaller mass of metal in the riser a freezing-off occurred before the more impervious cores had been penetrated to a considerable extent. This test also shows that metal head alone can cause penetration unless the core is made fairly impervious by the addition of some material such as

silica flour.

The Bleed-Off test, in which no penetration occurred when 450 lb of metal were poured into the 5-in. casting, indicates that heat without the presence of an appreciable metal head is not of itself sufficient to cause penetration.

In the core temperature tests described above the quartz tubes were placed 1/8 in. below the surface of the sand. To determine whether similar results could be obtained at the core surface, tests were made with the 5-in. casting in which the quartz tubes were placed at the surface of the cores of Series B. The curves obtained in this test are shown in Fig. 13. It will be seen that the results obtained are almost identical for cores No. 1B, 2B and 3B. Core No. 4B (100 per cent silica flour), however, has a smaller temperature lag and attains a higher temperature than in the previous test 1/8 in. below the surface. This could possibly be due to the small particles of silica flour fusing and forming an impervious mass at the surface. If this latter conception is correct it could also contribute to the increased resistance of 100 per cent silica flour cores to metal penetration.

Grain Fineness and Screen Distribution

As outlined above, the cores used in the 50-in. casting required large additions of silica flour to prevent metal penetration. Another method of obtaining a similar effect would appear to be to use finer sand, without the addition of silica flour. Special sands were prepared by selectively screening subangular New Jersey foundry sands, to determine the degree of fineness necessary to prevent penetration (Sands 4 to 11 inclusive, Appendix I). These sands were used in preparing cores series D and E (Appendix II). The D series has a narrow range, and the E series a wide range screen distribution. Core 1D has approximately the same average screen size as core 1E, core 2D corresponds to core 2E and so on. Cores 1D, 2D, 3D and 4D, and 1E, 2E, 3E and 4E are in decreasing order of screen size, with a difference in size of about 1 screen $(\sqrt{2})$ between each number, except in the case of No. 4D and 4E. It is difficult to obtain selective screen distribution in fine grain sizes. The results of this test are outlined in Table 6.

Table 6-Effect of Grain Fineness and Screen Distributions

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| Core No. | 30-in. Casting 2950 F | 50-in. Casting 2950 F |
|----------|---|--------------------------|
| 1D | Completely penetrated | Not done |
| 2D | Penetrated 1/2 way, plus bad veining | 92 29 |
| 3D | 3/32 in. around side, plus bad veining | Completely penetrated |
| 4D | Burn-on | Burn-on |
| 1E | Completely penetrated | Not done |
| 2E | | 50 99 |
| 3E | 1/8 in. around side, plus bad veining | Completely penetrated |
| 4E | Some burn-on, slight veining | Bad burn-on |

The test did not reveal any significant difference in penetration of wide and narrow range sands with the same average screen size. It appears, however, that with this type of sand (subangular) the average fineness should be less than 0.075 mm to prevent penetration in castings with a high metal head. Similar tests should be carried out with round-grained sands.

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Effect of Core Wash

Unwashed cores were used in the above tests. To obtain an indication of the efficacy of a normal foundry core wash in preventing penetration, a test was made in which the test cores were washed with a 50-Baume silica flour wash. Rammed cores (three rams) of the silica flour series (cores 1B, 2B, 3B) and a 2B core rammed 50 times were used in a 50-in. casting, and poured at 2950 F. The results of this test are in Table 7, together with those previously obtained with unwashed cores (Table 5). It will be noted that core wash did not prevent penetration except with core No. 3B, where minor improvement was observed. In this casting, the 2B core rammed 50 times was slightly better than the standard-rammed core. This core was inserted because no further information could be obtained by washing a 100 per cent silica flour core

TABLE 7-EFFECT OF CORE WASH

| Core No. | Washed | Unwashed (from Table 5) |
|---------------|--|-------------------------|
| 1B | Completely penetrated | Completely penetrated |
| 2B | Penetrated 2/3 way | Penetrated 3/4 way |
| 3B | Burn-on, plus slight veining | 3/8 in. at inside wall |
| 2B 50 rams | 1/4-3/8 in., mainly around inside corner | Not done |

Effect of Wood Flour

All cores used in the above tests evidenced some degree of cracking when subjected to heat shock at 2500 F (1370 C). It was believed that this cracking would help the metal to penetrate by providing planes of easy passage to the center. Wood flour was used to prepare core mixtures which did not crack under heat shock. (Series C, Appendices.) All cores of this series penetrated, both in the washed and unwashed condition when used in the 30-in. casting poured at 2950 F.

When a 15-in. high casting was used, however, wood flour produced a definite improvement by preventing veining and penetration. Cores 2C and 4C used in this casting contained wood flour and did not crack under heat shock at 2500 F. From the results of these tests (Table 8 and Fig. 14) it appears that further investigation of the combination of wood flour core mixtures and core wash would be in order.

TABLE 8—EFFECT OF WOOD FLOUR 15-in. Casting, 2950 F

| Core No. | Results | |
|--------------|----------------------|--|
| 1C, washed | Bad veining | |
| 2C, unwashed | 3/16-in. penetration | |
| 2C, washed | Good | |
| 4C, washed | Good | |

Summary

1. Gassy metal can cause metal penetration.

2. Ferrostatic pressure was found to be an important cause of metal penetration.

3. No significant beneficial effect could be observed by increasing the number of rams given the test specimens from 3 to 50.

4. A slight increase in penetration was obtained

by increasing the pouring temperature over 2800 F to 3080 F.

5. The thermal curves, the Bleed-Off and Metal Head tests indicate that core temperature is not as important as ferrostatic pressure in producing metal penetration.

6. Cores made with 100 per cent silica flour resisted penetration under the highest ferrostatic pressure used in these tests.

7. No significant difference in penetration of wide and narrow range sands was observed when the average screen size was the same. In castings with a high metal head it appears that for a subangular sand the average fineness should be less than 0.075 mm to prevent penetration.

8. A normal silica flour wash was not effective in reducing penetration.

9. With a medium metal head (15-in.) a washed wood flour core helped to prevent penetration.

It is not intended to conclude from this work that ferrostatic pressure is the sole cause for metal penetration.

This investigation, however, does introduce an effective method of testing metal penetration. Increasing the head of the standard casting increases the severity of the test. Therefore, this technique can be used to evaluate the relative importance of different variables. Further work both corroborative and new is planned.

References

1. S. L. Gertsman, "Research and Development in Canadian Steel Foundry Practice," Preprint Paper No. NI. 7, Fourth Empire Mining and Metallurgical Congress, Great Britain (July 1949), 30 pp.

1949), 30 pp.

2. A. E. Murton, "Investigation of Metal Penetration of Steel Core Sands," Part I, Investigation No. 2284. Department of Mines and Resources, Bureau of Mines, Canada (September 8, 1947), 15 pp.

DISCUSSION

Chairman: C. W. Briggs, Steel Founders' Society of America, Cleveland.

Co-Chairman: R. E. Kerr, Pettibone-Mulliken Corp., Chicago. Geo. E. Dalbey: 1 Has the author found any marked evidence of gas disturbance on the surface of the 100 per cent silica flour core?

MR. GERTSMAN: If we had any gas disturbance we could not recognize it. We did get good results with the 100 per cent silica flour core. You normally would expect some gas. However, we



Fig. 14-Effect of wood flour and core wash.

APPENDIX I-SANDS USED IN PENETRATION TESTS

| | Sand No | . 1 | 2 | 3 | 4 | 5 | 6 | 7 | 8 | 9 | 10 | 11 |
|-----------------------|-------------|---------------|----------|-----------------------|-----------|--------------|------|-------------|----------|----------|------|------|
| | Source | New Jersey | Kingston | Crushed Serpentine | (Sands 4- | 11 Inc. made | from | selectively | screened | samples) | | |
| U.S. Screen No. | Approx. Dia | m., | | | Per C | ent Retained | | | | | | |
| 20 | 833 | | _ | See Note A | _ | num . | _ | _ | - | - | _ | - |
| 30 | 589 | 0.4 | 4.2 | - | 0.1 | | _ | | 4.7 | 0.5 | 0.1 | - |
| 40 | 417 | 2.0 | 33.3 | | 0.5 | - | _ | - | 9.0 | 5.8 | 0.4 | 0.1 |
| 50 | 295 | 10.8 | 36.7 | | 25.9 | 0.2 | 0.1 | - | 19.6 | 9.2 | 5.1 | 0.5 |
| 70 | 208 | 37.4 | 16.2 | | 50.9 | 21.1 | 0.9 | - | 28.4 | 18.5 | 8.2 | 1.8 |
| 100 | 147 | 43.6 | 7.0 | | 21.2 | 50.8 | 25.2 | 0.1 | 21.4 | 30.7 | 19.5 | 8.1 |
| 140 | 104 | 5.4 | 1.6 | | 0.7 | 22.9 | 49.5 | 0.8 | 10.5 | 18.7 | 27.6 | 28.0 |
| 200 | 74 | 0.4 | 0.4 | | 0.4 | 4.6 | 21.1 | 35.8 | 5.6 | 11.0 | 22.7 | 31.6 |
| 270 | 53 | Tr | | | 0.1 | 0.3 | 2.4 | 35.9 | 0.4 | 3.1 | 10.1 | 19.9 |
| Pan | 20 | Tr | _ | | 0.1 | 0.1 | 0.8 | 27.3 | 0.4 | 2.5 | 6.4 | 9.8 |
| A.F.S. | Clay | _ | - | | - | - | _ | | - | _ | _ | - |

Note A — Sand 4A was made from crushed serpentine rock, which was being tested as a molding and core sand at the beginning of this investigation. Modifications were made in the composition of this core as used in the tests recorded in Tables 1, 2, and 3, although for the same test the composition remained constant. The binder content ranged from 1½ per cent oil to 2 per cent phenol-formaldehyde, and the baking temperature was modified accordingly. The screen distribution of this sand was also varied, ranging from 30 to 50 A.F.S. fineness number.

APPENDIX II-MIXTURES USED IN PENETRATION TESTS

| Mix No. | Sand No. (Appendix I) | Moisture, per cent | Sand | Cereal | Oil | Ingredients (p Silica Flour | arts by weight Wood Flour | |
|------------|-----------------------------|-----------------------|-------------|--------|-----|--------------------------------|------------------------------|-----|
| IA | 1 | 10.5 | 600 | 10 | 15 | 400 | _ | _ |
| 2A | 2 | 3.9 | 1000 | 30 | 15 | | - | |
| 3A | 2 | 3.9 | 1000 | 10 | 15 | - | 10 | - |
| 4A | 3 | (See No | te A Append | dix I) | | | | |
| 1B | 1 | 3.4 | 1000 | 10 | 10 | _ | - Armeter | |
| 2B | 1 | 8.5 | 750 | 10 | 10 | 250 | - | - |
| 3B | 1 | 10.8 | 500 | 10 | 10 | 500 | december . | |
| 4B | | 13.5 | | 10 | 10 | 1000 | - | - |
| 10 | 1 | 3.0 | 1000 | _ | 30 | _ | _ | . – |
| 2C | 1 | 6.5 | 1000 | - | 30 | _ | 20 | |
| 3C | 1 | 3.0 | 1000 | - | 30 | _ | - | 15 |
| 4C | 1 | 6.5 | 1000 | _ | 30 | _ | 20 | 15 |
| 1D | 4 | 3.5 | 1000 | 10 | 10 | _ | _ | _ |
| 2D | 5 | 3.5 | 1000 | 10 | 10 | _ | | _ |
| 3D | 6 | 3.5 | 1000 | 10 | 10 | _ | - | _ |
| 4D | 7 | 3.7 | 1000 | 10 | 10 | - | _ | _ |
| 1E | 8 | 3.0 | 1000 | 10 | 10 | - 1 | _ | _ |
| 2E | 9 | 3.0 | 1000 | 10 | 10 | | _ | - |
| 3E | 10 | 3.5 | 1000 | 10 | 10 | - | _ | _ |
| 4E | 11 . | 3.5 | 1000 | 10 | 10 | _ | | _ |

got a very smooth surface with the 50-in. high casting. Whether that will hold for any higher heights I cannot say.

CHARLES LOCKE: Penetration is shown to be a mechanical effect and if the close locking or the close contact of the grains are disturbed and moved penetration can occur. Such disturbance is often caused by the movement of the liquid steel at the gate area. I think the test, as it was devised, did not provide for this. Even the test procedure in which a flow off was provided allowed the core to be completely covered by metal at all times. If alternate covering and exposure took place a rough casting surface usually results and the penetrating phenomena is approximated.

Mr. Gertsman: One of the reasons for the bleed-off test was to isolate ferrostatic pressure, to see whether by increasing the height of our castings we were not just passing more metal by the core and thus causing penetration by erosion. We wanted to see whether it was ferrostatic pressure that was causing increased penetration when we increased the height of the casting or whether it was some other factor that was operating.

In the casting designs we introduced the metal quietly in order to avoid the possibility of turbulence or erosion being an extraneous factor in our work.

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R. E. Morey: About four years ago we did some work at the Naval Research Laboratory on bronze penetration. The work reported by Mr. Gertsman verifies some of the points that we have brought out at that time. One of these is that penetration and veining are entirely separate defects and have separate cures.

We cured veining by the addition of wood flour. Penetration was prevented as was done in Mr. Gertsman's experiments by washing the mold to provide a mechanical barrier at the mold surface.

The illustration showing the penetration of the steel in the

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² Supervisor, Foundry Process Research, Armour Research Foundation, Chicago.

⁸ Metallurgist, U. S. Naval Research Laboratory, Washington, D. C.

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APPENDIX III-PROPERTIES OF CORE MIXTURES

| Core | Moisture, | Green | Green | Floor Minor | Baked Tensile | Hot Str | | 30-1b | Wt. of 11/8-in. |
|------------|-----------|--------------|------------------------------|-------------|------------------|-------------------|--------|-------------------------------|-----------------|
| No. | per cent | Permeability | Compressive Strength, psi | | Strength, psi | psi (12 1000 F | 2000 F | Collapsibility 2000 F, sec | grams |
| 1A | 10.5 | 11 | 3.8 | 77 | 220 | 210 | 227 | _ | 64 |
| 2A | 3.9 | 243 | 0.49 | 89 | 190 | 68 | | 37 | 55.5 |
| 3A | 3.9 | 250 | 0.48 | 88 | 73 | 16 | | 25 | 54.5 |
| 4A | 4.2 | 300-400 | _ | - | 55-203 | - | _ | _ | 52-57 |
| 1B | 3.4 | 117 | 0.46 | 89 | 139 | 27 | | 27 | 53.5 |
| 2B | 8.5 | 42 | 2.5 | 82 | 246 | 139 | 138* - | 54 | 61.0 |
| 3B | 10.8 | 7.7 | 4.8 | 77 | 175 | 132 | 161* | 143 | 62.0 |
| 4B | 13.5 | 4.2 | 6.5 | 76 | 73 | 98 | 138* | 81 | 59.5 |
| 1C | 3.5 | 115 | 0.37 | 88 | 262 | 120 | 30 | _ | 54 |
| 2C | 5.0 | 102 | 0.50 | 88 | 122 | 34 | _ | 40 | 52.5 |
| 3C | 3.5 | 115 | 0.41 | 86 | 305 | 144 | 61 | | 53.5 |
| 4C | 5.0 | 119 | 0.44 | 86 | 93 | 23 | - | 33 | 52 |
| 1D | 3.5 | 185 | 0.49 | 88 | 143 | 17 | - | 28 | 51.5 |
| 2D | 3.5 | 93 | 0.62 | 88 | 152 | 23 | - | 37 | 50.0 |
| 3D | 3.5 | 63 | 0.65 | 86 | 42 | 26 | _ | 28 | 53 |
| 4 D | 3.7 | 20 | 1.14 | 83 | 88 | 38 | _ | 33 | 45.5 |
| 1E | 3.0 | 102 | 0.64 | 88 | 105 | 36 | _ | 54 | 53.5 |
| 2E | 3.0 | 68 | 0.82 | 88 | 125 | 39 | _ | 139 | 52 |
| 3E | 3.5 | 46 | 0.80 | 86 | 51 | 82 | - | 62 | 54 |
| 4E | 3.5 | 26 | 1.02 | 84 | 114 | 49 | | 53 | 47 |

APPENDIX IV—PROPERTIES OF SPECIALLY RAMMED CORES

| Mix No. | No. of Rams | Green Permeability | Green Compressive Strength, psi | Weight of 11/8 in. specimen, gm |
|------------|----------------|-----------------------|---------------------------------------|---------------------------------------|
| 1A | 3 | 10.5 | 4.7 | 60 |
| 1A | 5 | - | - | 61.5 |
| 1A | 7 | _ | - | 62.5 |
| 1A | 10 | 5.0 | 8.2 | 63.5 |
| 1A | 20 | 3.0 | 12.0 | 66 |
| 1A | 50 | 2.7 | 19.0 | 68 |
| 2A | 3 | 226 | 0.55 | 53 |
| 2A | 5 | _ | _ | 54 |
| 2A | 7 | _ | _ | 54.5 |
| 2A | 10 | 173 | 0.92 | 55 |
| 2A | 20 | 151 | 0.95 | 56 |
| 2A | 50 | 134 | 1.2 | 59.5 |
| 3A | 3 | 212 | 0.67 | 53 |
| 3A | 5 | | _ | 54 |
| 3A | 7 | - | _ | 54.5 |
| 3A | 10 | 146 | 1.2 | 55 |
| 3A | 20 | 128 | 1.5 | 56 |
| 3A | 50 | 112 | 1.7 | 59.5 |
| 2B | 50 | 21.8 | 8.1 | 64.5 |

unkilled or rimming condition was very interesting because that parallels our work in which we found that a bronze containing dissolved garses penetrated quite badly and a degassed bronze did not penetrate. In rimming or unkilled steel you have a similar gas condition. In bronze we found that rejection of gas from solution caused the interdendritic liquid to exude into the sand under the force of that gas. That was one of the causes of the penetration.

There is quite a difference in the fluidity of steel in the killed and unkilled condition. It may have some effect on the penetration of the metal into the sand.

J. B. CAINE: ⁴ There are some points that I would like to bring up merely as a matter of clarification and hoping for new work.

The author is to be highly commended for this excellent contribution to our knowledge of foundry sands. The specimens in this paper were rammed between three and fifty rams using a core sand. With the very flowable core sands used, even three rams is quite a hard ram. It is quite possible that the effect of ramming will be more pronounced than shown in this paper if a less flowable sand is used, for example, a clay or bentonite-bonded molding sand. Therefore the question of ramming level and rammed density is not settled as yet.

This point in no way conflicts with the important point established by this paper that over ramming does not decrease penetration.

Another point is that of sand-metal ratio. The test specimen that the authors are using is very good in that it is severe, with a low sand-metal ratio. The sand-metal ratio in most castings is much higher and penetration can be prevented with smaller silica flour additions than those used by the authors. This is fortunate, for very high silica flour additions create another problem—hot tearing.

In the original work on metal penetration done ten years ago by the writer, a different type of test specimen was used. The hydrostatic head on that particular specimen was 13 in. Standard 2-in. diam by 2-in. A.F.S. test specimens were used, extending into the casting for a shorter distance than those of this investigation. Therefore, although the results show the same trend as those of this paper a quantitative check is not to be hoped for because the conditions are different.

Mr. Gertsman: We have not recommended the use of 100 per cent silica flour cores. We wanted to get as low a permeability as possible and we used 100 per cent silica flour to get it.

I have heard, however, of several foundries overcoming a metal penetration problem where they had a high ferrostatic head on the casting. They solved it by using a 100 per cent silica flour core.

A. J. Kiesler: We conducted similar tests as did Mr. Gertsman. Our test casting was a cylinder 36 in. high and 12 in. in diam with a swirl gate at the bottom.

In answer to Mr. Locke's question of penetration due to running metal, our method measured this effect.

We did not vary the hydrostatic head; our studies concerned sand formulations.

We found that by proper gradation of the sand particles penetration could be eliminated and core washes were not necessary.

⁴ Consultant, Wyoming, Ohio.

⁸ Chief Metallurgist, Foundry Div., General Electric Co., Schenectady, N. Y.

WAGE INCENTIVE ADMINISTRATION

By Charles T. Hassell*

FOR MANY YEARS management and labor have been jointly interested in improving the American standard of living. However, they have learned by recent experience that higher wages, if followed by higher prices, is not the solution of their problem. Under this condition purchasing power remains unchanged and the standard of living is not improved. Consequently, they are now seeking ways to widen the difference between the level of wages and the level of prices.

Management is seeking ways to reduce costs and selling prices, earn a reasonable profit, and at the same time at least maintain the present wage level. Labor, on the other hand, is seeking higher wages and, in some instances, has proposed that the cost of these increases be paid out of corporate profits, rather than

passed on to the consumer.

This proposal by labor might be a solution of the problem if the profits of each company in industry were sufficient to absorb the cost of further wage increases and still leave adequate funds for working capital, reserves, and the payment of dividends. Unfortunately, very few companies in the foundry industry are in such a favorable position. On the contrary, due to declining volume and prices, the situation is quite the opposite. Therefore, many foundrymen must increase their profit margin if their businesses are to survive and prosper.

If management and labor are to gain their objectives it must be through increased production per man-hour and lower unit costs. This is the only sound basis for higher wages and lower selling prices. In their endeavor to achieve lower costs and higher wages, management and labor should cooperate and make a sincere effort to use time study and its sequel, the wage incentive, to better advantage than they have

in the past.

The basic principle involved in a wage incentive plan is payment of wages on the basis of output rather than by the hour. The purpose, of course, is to induce the worker to produce more and thus earn more than he would otherwise and, at the same time, lower the cost per unit of production.

Management's Responsibility

Wage incentives have been used in industry for nearly 70 years. However, they have not always accomplished their purpose. In some plants the results have been entirely satisfactory. In other plants the plan has been a complete failure. Perhaps then if we learn what contributes to success, and what causes failure, we will have made some progress toward solving management's and labor's problem.

The success of a wage incentive plan, like that of any other major project, depends largely on top management. In the development and administration of the plan authority and responsibility must necessarily be delegated to various members of the organization. Nevertheless, it is still management's responsibility to see that objectives are achieved.

Management must then be watchful to see that details essential to success are not overlooked or neglected. If weakness or negligence is apparent at any time, prompt action must be taken to correct the condition. Unless this is done the condition becomes malignant and failure is inevitable.

Cooperation of Supervision and Labor

To accomplish its purpose a wage incentive plan must have the approval and full cooperation of both supervision and labor. Each of these segments of the organization often approve and promise to cooperate, even though they do not fully understand the plan nor know what their responsibilities are. Needless to say, commitments made under these conditions are seldom fulfilled. Management should, therefore, initiate and carry out a program for instructing both the foremen and labor's representatives in the basic principles of time study, how the rates are determined, and the part that each of them must play in the administration of the plan.

Some foremen believe that the use of wage incentive relieves them of all responsibilities so far as production per man-hour is concerned. This supposition is false. Although the wage incentive does offer an inducement for the workman to produce more, the foreman must be always watchful and maintain standard working conditions. Flasks, patterns, machines, core boxes and all other equipment must be frequently inspected and repaired if they are in poor condition.

All delays beyond the control of the workman must be held to a minimum; otherwise he is unable to produce to the extent of his ability and has just cause for grievance. These grievances, when processed, result in either rate adjustments or special allowances. In either case the final result is higher unit costs.

The wage incentive is by no means a one-sided affair. Labor, as well as management, must make a contribution if success is to be assured. Labor's contribution is more output per man-hour. The officers and shop stewards of the local union should then cooperate with management and explain to the workmen that wage incentive means more pay for more work, and that only by producing more than the fair standard for normal effort, which is determined by time study, can they earn higher wages.

The Need for Standards

Payment of wages on the basis of output necessitates rate setting. These rates are expressed either in terms of time or money per unit of production, depending on the type of incentive plan used. In either case they should be based on standards for normal effort.

Standards for normal effort are usually called "production standards." They express the amount of work that should be done per hour if wages are paid on

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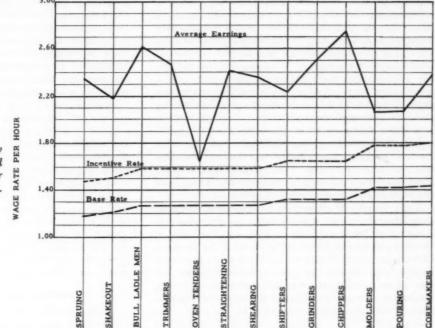


Fig. 1-Graph plotted to show base rate, incentive rate, and average hourly earnings for each job under incentive rates.

the basis of time rather than output. These standards, if properly determined, should not be changed unless the method of doing the job is changed to the extent that either more or less work is involved.

Production standards are then the foundation of the wage incentive. They not only influence wages, but also costs and selling prices. Consequently, they must be fair to both labor and management. Fair standards cannot be determined by mere guesswork, but should be determined by competent time-study men using approved time-study methods.

Time-Study Procedure

So much has been written about time-study techniques that it would take considerable time to adequately present a further contribution on the subject. However, some essential details which should not be overlooked or neglected by time-study men will be first mentioned and later emphasized. They are as follows:

- 1. Time study should be preceded by process analysis.
- 2. Each element of work should be timed separately instead of timing the job as a whole.
- 3. Rates should be supported with detailed specifications of method and work to be done.
- 4. Adequate procedures for counting production should be provided.
- 5. The reasons for downtime and special allowances should be reported.

Unless time study is preceded by process analysis, the proper time for doing the job cannot be determined. Process analysis, in this case, means checking the process before the job is timed to see that everything affecting time is in order.

All the equipment used in the operation must be in good working condition. This may include molding or core machines, flasks, patterns, core boxes and power tools. Sufficient power or air to properly operate the machines must be available. The working area must be neat and orderly. Tools, supplies, flasks, bottom boards and core plates must be easily accessible so that they can be obtained without undue effort on the part of the workman.

Next, but equal in importance to checking the equipment, comes checking the elements of work required to do the job properly. In molding, for example, elements such as riddling, peening, and venting should not be left to the discretion of the molder but should be decided by the foreman.

Timing each element of work involved in an operation is generally accepted as better than timing the job as a whole. The purpose of time study is to determine the normal or standard time required to do the job. It is doubtful, at least in the author's mind, if this can be done by applying a rating factor, which is the result of judgment by the time-study man, to the over-all time. Instead, he should break the job down into elements or work motions and then record the time for each element. By analyzing time studies that are made in this manner the time for similar elements can be compared with other time studies and thus minimize guesswork in rating the workman's speed or effort.

The foregoing method has several other advantages. First, data can be collected and compiled in such a manner that standard times for many operations, including coremaking, molding, shearing off gates, grinding and straightening, can be set before the jobs are put into production. This is especially advantageous for foundries that have small lot orders which may be processed before the operation can be timed.

Second, the use of element standard times enables the time-study department to quickly and equitably adjust rates, without further time study, if the method of doing the job is changed to the extent that elements of work are either eliminated from or added to the job.

Third, the use of such data results in the standard times for all jobs having the proper relationship between them according to the work content of each job. This is very important because the fairness of rates is usually determined by comparing the rate for

one job with that for a similar job.

Fourth, element standard times can be used to predetermine rates for new methods of operation. Among the most successful mechanization programs that the author has seen were those where the rates for molding, pouring, shifting and shakeout were set from element standard times before the mechanized unit was put into operation. This avoided operating on a day-work basis and overmanning the operation until the various jobs were timed and new rates set.

Rates should be supported with written specifications describing the equipment to be used and explaining in detail the work to be done. The work to be done can best be described by showing the elements of work and their respective standard times that were used in determining the total standard time for the job. These specifications not only serve as instructions for the foremen and workmen, but also provide factual data for settling disputes over rates if they arise.

If wages are based on output, it is obvious that the units produced must be accurately counted before correct payment can be made. Nevertheless, some foundrymen are negligent and do not obtain accurate

counts after each operation.

Accurate counts can usually be obtained by the use of well organized procedures without incurring any additional expense. In some cases the purchase of an additional scale or the employment of a clerk may be required. Even so, the results will probably fully warrant the expenditure.

Payment for downtime or time lost by an incentive worker due to delays beyond his control is costly. Furthermore, it is the subject of many grievances and controversies between supervision and workmen. In order to minimize the downtime, the plant manager must first know the causes. Procedures should then be

developed to give him that information.

These procedures include the use of forms on which the foremen write in the workman's name, the amount of lost time, and the reason. Each foreman must cooperate in this undertaking and clearly state the reason even though he is at fault. These reports then provide the payroll department with the information needed to make correct payment and also give the manager the information needed to correct the causes and reduce downtime to an absolute minimum.

Organizing the Time-Study Department

Since the success of the wage incentive depends so much on time study, management should be careful when selecting the personnel for this work. It is not a job that can be done by men who have had neither previous experience nor special training. Yet we know of cases where management has delegated the responsibility of time study to men who did not know how to do the work and were not especially interested in learning.

To do the job well, a time-study man must first of all be interested in his work. In addition, he must possess not only the technical knowledge necessary to make and analyze time studies, but also certain personal characteristics. He must be able to get along with other members of the organization. Moreover, since his activities influence management-labor relations, he must gain the confidence of labor in his integrity. He must, therefore, be able and willing to tactfully answer all questions pertaining to time study.

The procurement of competent time-study personnel is a difficult problem, especially in small foundries where the activities may only require the services of one man. Nevertheless, it makes no difference whether the personnel consists of one man or ten. It still must

be fully qualified to do the job properly.

If management does not have fully qualified men in the organization it should engage professional services to organize its time-study department and give its personnel some preliminary training. These services should later be supplemented with periodic services for the purpose of checking the activities of the department and helping solve some of the difficult problems as they arise.

Extended Use of Wage Incentives

Many foundries limit the use of wage incentives to coremaking and molding. This is not always wise because the other employees may feel that they too should have the opportunity to carn higher wages. Unless they are given this opportunity they will probably demand higher base rates without proportionately increasing their output. Management should then seek to improve its relationship with labor by developing sound incentive plans for as many operations as possible.

Sound incentive plans can be developed to cover almost every operation in the foundry. Individual plans, whereby each workman is paid in proportion to his own efforts, should be used wherever the production of individuals can be counted. These operations vary according to the type of foundry. They generally include molding, coremaking, core assembly, shearing, grinding, chipping and straightening. Group plans, whereby each workman is paid in proportion to the effort of the group as a whole, have been successfully used for operations such as melting, pouring, shifting, shakeout, tending ovens, sorting, and shipping.

The use of incentives is by no means limited to production workers. They can likewise be used successfully to obtain better effort on the part of supervisory personnel. Some managements distribute a portion of the annual earnings to the key men of the organization—including foremen—on a more or less arbitrary basis. While this type of incentive may invigorate the top-flight executives, it is doubtful if it stimulates better effort on the part of the foremen.

In the first place, many items over which the foreman has no control influence profits. For example, selling prices, volume, selling and administrative expense, and fixed charges all affect the profits of a company. As the foreman is not concerned with such items, he may believe that the payment of the incentive is a matter of chance instead of being dependent on his efforts.

In the second place, incentive payments that are made annually are too far apart to cause very much be but all

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enthusiasm except once a year. To be really effective, incentives for supervisors should be based on the performance of the men who participate in the plan. The performance of each department can be measured by comparing the actual amounts with standard allowances for labor, scrap, downtime, and indirect labor. Gains or losses over these standard allowances are then evaluated to determine the amount of incentive earned by extra effort on the part of the foreman.

Whether the incentive should be based on the individual performance of each foreman or on the combined performance of all the foremen is debatable. Since the successful operation of a foundry depends so much on the cooperative effort of all the foremen, we believe that the results of each department should

TABLE I—BASE AND INCENTIVE RATES, AND AVERAGE EARNINGS

| Job Classification | Base Rate | Incentive Rate | Average Earnings | Average of Base, % |
|-----------------------|--------------|-------------------|---------------------|-----------------------|
| Spruing | 1.18 | 1.48 | 2.35 | 200 |
| Shakeout | 1.21 | 1.51 | 2.19 | 181 |
| Bull Ladle Men | 1.27 | 1.59 | 2.62 | 206 |
| Trimmers | 1.27 | 1.59 | 2.47 | 195 |
| Oven Tenders | 1.27 | 1.59 | 1.65 | 130 |
| Straightening | 1.27 | 1.59 | 2.41 | 190 |
| Shearing | 1.27 | 1.59 | 2.36 | 186 |
| Shifters | 1.32 | 1.65 | 2.24 | 170 |
| Grinders | 1.32 | 1.65 | 2.51 | 190 |
| Chippers | 1.32 | 1.65 | 2.74 | 208 |
| Molders | 1.42 | 1.78 | 2.06 | 145 |
| Pouring | 1.42 | 1.78 | 2.07 | 148 |
| Coremakers | 1.44 | 1.80 | 2.38 | 165 |

be measured separately, in order to detect weakness, but that incentive payment should be based on overall results.

The wage structures of many companies contain inequities which are the result of either the foremen setting the base rates in their respective departments by arbitrary judgment, or by collective bargaining between management and the workers without the aid of job analysis. In the development and administration of a wage incentive plan it is just as important to have sound and equitable base rates as it is to have sound and equitable production standards. It is the combination of these two factors that determine the rate of pay per unit of production and govern wages. The full cooperation of a workman is unlikely unless he believes that his base rate is fair in comparison with the rates for other jobs. If he should ask why job A pays three cents per hour less than job B, management should have data in clear and complete form to substantiate the three-cent differential.

Job evaluation provides management with that type of information. Job evaluation is a technical procedure that requires experience and fairness. It involves analyzing each job and grading its essential characteristics in a systematic way to determine its value in relationship to all other jobs. When properly done, job evaluation provides labor and management with a yardstick to determine the base rates for each job in

In order to clearly show the results of bad practices in the development and administration of a wage incentive plan, Fig. 1 is presented as a graphic picture of an actual case history. The graph shows the base rate, incentive rate, and the average earnings per hour for each job where incentives were used. The points that are plotted on this chart are also shown in Table 1.

The base rate for each job was determined by collective bargaining between the company and the workers. These are in line with the industry rates and reflect fairly well the proper differentials between jobs. The incentive rates were also the result of collective bargaining and are 25 per cent higher than the base rates. Before going further, let us examine the significance of incentive rate.

Incentive rate means the amount of money per hour that an incentive worker can earn by applying more effort than is expected of him if he is paid by the hour rather than on the basis of output. In actual practice, this will vary for individuals according to the degree of skill that they possess and the amount of effort that they apply. The incentive rates shown in Table 1 represent what can be earned by the average worker. In this case, it means that the rates per unit of output are set at such a level that the average worker can earn approximately 25 per cent more than his base rate.

Example of Poor Administration

Let us now look at average earnings. The amounts shown in Table 1 and on the chart (Fig. 1) are not the highest for individuals, but are the average earnings per hour for all the men who worked at the operation. These amounts can then be considered as the actual earnings of an average worker.

The relationship between average earnings and base rates are expressed percentagewise in the last column of Table 1. They range from 130 per cent of base rate for oven tenders to 208 per cent for chippers.

Two conclusions can be derived from this picture. First, that all of the workmen engaged in the operations were far above average. Second, that the rates per unit of output were improperly established. The first conclusion would be false, because the production per man-hour in this plant was among the lowest in the industry for the comparable class of work. This wage incentive plan then accomplished only one of the three purposes which should be management's and labor's objective. It resulted in higher wages but failed to gain greater output and lower unit costs.

Before discussing the effect that these high wages and low production had on the affairs of the company, let us discuss profit. The object of every business is to make a profit. Profit, then, should not be considered as a leftover but should be carefully planned and be the first instead of the last consideration of management.

Profit planning begins by examining the behavior of each item of expense and resolving them into their variable and fixed portions. However, in business we deal with three classes of expense. These must ultimately be reduced to two:

Fixed expense—items which do not change with activity.

Variable expense—items which increase or decrease in proportion to activity.

3. Mixed expense—these items are partly variable and partly fixed. Therefore, it is necessary to separate the mixed items into their variable and fixed portions

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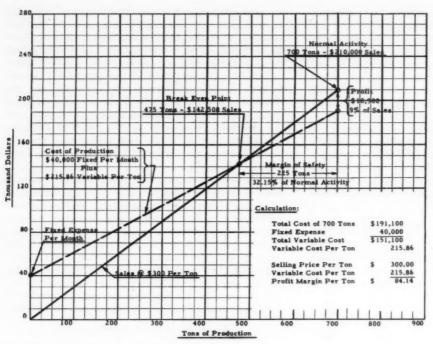


Fig. 2—This chart illustrates the use of the total fixed expense in planning the profit for a malleable foundry having a fixed expense of \$40,000 per month and production capacity of 900 tons per month.

so that the fixed portion may be added to the amount of expense for the wholly fixed items. By this process we have finally resolved all expense into two classes, namely, fixed and variable.

Figure 2 shows how to use the total fixed expense in planning the profit for a malleable iron foundry that has \$40,000 per month fixed expense and a capacity of 900 tons per month. In this illustration, we have assumed that the objective of management is to make a profit equivalent to 9 per cent of sales when the plant is operating at 77.8 per cent of capacity. We have further assumed that the average selling price is \$300 per ton. The essential facts shown on the chart are then based on producing 700 tons per month (having a sales value of \$210,000) and making a profit of \$18,900.

Must Know Profit Factors

The diagonal line beginning at zero on the vertical and horizontal scales of the chart and extending to a point representing \$210,000 on the vertical scale and 700 tons on the horizontal scale shows the sales value of production. It is obvious then that to make a profit of \$18,900 per month, the 700 tons must be produced at a total cost of \$191,100, or the difference between \$210,000 and \$18,900. In order to achieve this objective it is essential that management know:

1. What the variable cost per ton must be in order to make the expected profit.

2. The margin of profit per ton, which is the difference between the selling price and the variable cost.

3. The break-even point or the activity required so that revenue from sales equals cost of production.

The variable cost per ton is determined by dividing the difference between the total cost and the fixed expense by the tons produced. In the illustration (Fig. 2) this is shown as \$215.86. The margin of profit is then \$300.00 minus \$215.86 or \$84.14. In other words, if the planned profit is to be realized, man-

agement must be watchful to see the difference between the average selling price and the variable cost is not less than \$84.14 per ton.

The break-even point is determined by drawing another diagonal line on the chart (Fig. 2). This line begins at \$40,000 and extends to a point representing \$191,100 on the vertical scale and 700 tons on the horizontal scale. This line shows the cost of production. The point where it crosses the line that shows the sales value of production is the break-even point. On the chart the break-even point is at 475 tons and \$142,500 sales.

Between the break-even point and normal activity is the area of margin of safety. This is shown on the chart (Fig. 2) as 225 tons, which is 32.14 per cent of 700 tons. In other words, if the variable cost per ton is controlled and the average selling price remains at \$300 per ton, activity can decline 32.14 per cent below normal before losses would be incurred. On the other hand, if the average selling price declines, variable costs must be reduced to maintain a profit margin of \$84.14 per ton.

Returning to our case history, we find a company whose variable costs were \$240 per ton. This was due to poorly developed incentive plans and the lack of managerial controls. Thinking that additional volume would solve the dilemma, management quoted prices which resulted in reducing the average selling price per ton from \$300 to \$280.

Figure 3 shows the profit pattern of the company under these conditions. By drawing the two diagonal lines representing cost of production and sales revenue, we find that their paths would not cross until 1,000 tons per month had been produced and sold. This means that the break-even point was 100 tons beyond the capacity of the plant. The only solution to this problem is a reduction in variable costs which, in this case, means greater production and decreased labor costs.

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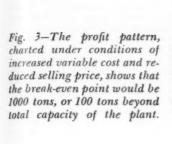
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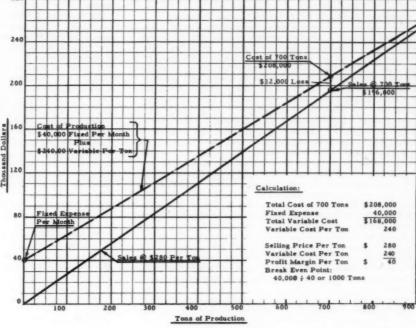
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Thus far the discussion on time study has pertained to wage incentives, but it can be used to advantage in supplementing other activities in the organization—estimating, cost control, and scheduling.

Time Study Aids Other Activities

Estimating: In estimating the cost of producing a casting consideration must be given to the cost of material, cost of direct labor, and the indirect expense. It is in estimating the cost of direct labor and the indirect expense that a well organized time-study department can make a worth-while contribution.

In order to estimate the cost of direct labor within a reasonable degree of accuracy, it is first necessary to thoroughly analyze either the drawing or pattern for the casting and then prescribe the exact process to be used for each operation. This should be done by the time-study and methods department in collaboration with the foundry superintendent. By using the combined knowledge of the time-study and manufacturing departments, the best and most economical methods are more likely to be determined. After the process for each operation has been prescribed, the time-study department can accurately determine the time and cost for each operation by the use of element standard times and standard base rates.

Indirect expense is estimated by the use of overhead rates which are determined by the cost department. These rates are usually based on past performance. As this type of expense includes indirect labor, rates based on past performance are often incorrect.

Overhead rates should be based on the amount of expense required to operate a department under good supervision. The time-study department should, therefore, determine the amount of indirect labor required for various levels of activity. With this information the cost department can then develop more accurate overhead rates for each department.

Cost Control: Stated briefly, cost control involves three major actions: (1) making a comparison between actual and estimated costs to determine what variations have occurred; (2) determining what caused the variation; (3) revising methods or practices to correct the unfavorable conditions.

The best way to simplify cost control is to prevent as many unfavorable variations as possible. No doubt, most of these are due to excess scrap, but many variations are due to poor utilization of labor. These can be prevented if the foreman has the proper information in regard to the number of men required for each job in his department. A progressive time-study department can provide that information by determining the proper crew size for each job in the plant.

Better utilization of labor can also be obtained by making work schedules in advance. To do this intelligently the works manager should know the amount of work ahead of each department in terms of manhours. He could then either increase or decrease his working force as required, and thereby minimize premium pay for overtime and also overmanning any of the operations. By using the production standards that were developed for wage incentives, all work ahead can be evaluated in terms of man-hours, providing information needed to make advance work schedules.

Scheduling: The time required to perform the operations must be planned in advance. Intelligent planning requires knowledge of: (1) amount of work, in terms of man-hours, that is ahead of each department; (2) time required to perform each operation necessary to manufacture the product; (3) lead time required on one operation before the next operation is started.

This information can be obtained through the use of production standards that were developed for wage incentives. Scheduling can then be done in such a manner that the manufacturing operations can be performed with minimum delay and confusion.

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DISCUSSION

Chairman: R. L. LEE, Grede Foundries, Inc., Milwaukee.

Co-Chairman: V. E. ZANG, Unitcast Corporation, Toledo, Ohio. ROBERT GREGG: I I am well pleased to note that A.F.S. has taken up this phase of the foundry industry. Regarding timestudy and costs to arrive at selling price, in most small foundries this is part of the superintendent's duties. He should be qualified more than anyone else to estimate the correct time necessary to do a job. I would like to ask the author whether he thinks 25 per cent bonus on day work wages is enough to attract the best workmen?

MR. HASSELL: In some cases I think it is and in other cases it is not. It depends a little on what has been the practice. I know that it is gradually increasing. I remember when 15 per cent was enough to get better effort. During the war we had 35 and in some cases 40 per cent. I think that 25 per cent is a good average. Some foundries might have to give 30 per cent.

CO-CHAIRMAN ZANG: In most instances the big problem today is in the small foundries with 10 to 20 molders at most. We have no time to make timestudies because we have to be on the job. We get a blueprint and usually it sets the time on the job and the selling price for the casting. The big problem especially in the smaller foundries is training of the superintendent of the foundry to have an understanding of the selling price of the castings being made. Superintendents do not have the least idea of what the selling price of a casting is to be and still management expects him to show a profit in the operation of their foundry.

What do you think of the value of motion economy, the principles of motion economy given the supervisor on the job? They assist the timestudy prior to making the timestudy and also watch the methods during the timestudy.

MR. HASSELL: They have a place in foundries that have large production. I am thinking of a foundry that has orders that seldom run for over two or three days on one pattern. I do not think that that kind of analysis is justified for that type of work, but in the automotive industry, where you are working the same pattern for many months and oftentimes a year, I think we should analyze the motions of the worker.

MEMBER: For a short order you need a similar pattern or one that is made the same. Perhaps you may have 200 patterns to run, would they not all use the same method?

MR. HASSELL: If you have 100 or more difficult jobs and each job is made on a squeezer in a 12 x 17 flask, the use of element standard times will result in the total standard time for each job being the same except variations due to setting cores or other special elements that may be required on some jobs but not on others. However, the standard time for the base elements, which are those elements that must be done to make any squeezer mold, will be the same for each of the 100 or more jobs.

CO-CHAIRMAN ZANG: For that reason, I think that the training in element and motion economy would apply even to a shorter time.

MEMBER: The various elements are the same regardless of the variety.

MR. HASSELL: I think that equipment, such as the dust bag for parting, riddle and cores, should be located so that the molder can get them with the least effort. However, I believe that such things as this can be accomplished without the use of micromotion study.

MEMBER: In the last several years I have heard specialists discussing a more intricate study than timestudy. I think it is able to piece together any operation without a stop whatever. It seems to me that every time I have heard it there was not enough consideration of the fatigue that would enter into molding.

MR. HASSELL: When I mentioned that each element of work should be timed separately, I meant that they should be timed while timing the overall cycle. I do not believe in timing one element and then skipping three or four elements before timing another. I believe that timestudies should be made by using the continuous method. This means that the watch runs continuously and the observer records the watch reading as each element is completed. The net time for each element is then obtained by subtracting each reading from the one following. In this manner you have the net time for each element and also the overall cycle time.

In regard to fatigue, I do not believe that it can be determined by time study. I have timed the same man at different hours of the day and could not detect any change in pace. It is my opinion that allowances for fatigue must be determined by good judgment and experience.

CHAIRMAN LEE: Application of the fatigue factor is a matter of past experience, knowledge of the operations and good judgment.

MR. HASSELL: I think that is a good part of it.

CHAIRMAN LEE: One of the previous questions referred to analysis of motion studies in processing. In the cope and drag work I think it is inessential .5 measure the overall because if one man is making the cope operation or the drag operation faster than another you have to reassign some jobs to the fast man so that the whole thing balances out.

E. C. Rein: a In our plant we are currently operating under a "Standard Way Work Plan." We have heard a lot about incentive systems, and yet it is not possible, at this time, to install such a system. Now the question is: Is it possible to operate a captive foundry efficiently and competitively without a wage incentive plan?"

MR. HASSELL: I do not think it is right for people to want to earn all the same. I think it is a human trait that we all want to earn according to our ability. I would try to sell them on the use of incentives.

¹ Foundry Manager, Reliance Regulator Div., American Meter Co., Albambra, Calif.

² Timestudy and Methods Dept., Ford Motor Co. of Canada, Ltd., Windsor, Ontario, Canada.

REPRODUCIBILITY OF FOUNDRY SAND TESTS

By

B. H. Booth, * P. C. Rosenthal ** and H. W. Dietert ***

ABSTRACT

In the past, the question has frequently been raised as to whether the sand testing methods and equipment advocated by the American Foundrymen's Society are of sufficient accuracy and reliability to insure sand tests being reproducible wherever made. A good paper on this problem, as it existed in 1940, was given at the A.F.S. convention in 1941, by Stanton Walker. The title of this paper was "Reproducibility of Tests of Foundry Sand." One of the conclusions reached in this paper was that tests applied to foundry sands are not reproducible with sufficient accuracy to permit practical use of specifications involving narrow tolerances. This conclusion, although, perhaps, justified by the test data, did not reflect the opinion of all sand technicians. Consequently, several years later, the A.F.S. Green Sand Properties Committee decided that the time had come when the whole problem of reproducibility of foundry sand tests should be further investigated. With this in mind, a program was organized wherein several widely separated laboratories were to have the opportunity of testing identical samples of a prepared sand. After these tests were completed, and studied by the committee, a tour of the several laboratories was made by two of the committee members to see if they could determine the reasons for the discrepancies in data that were found to exist. Information developed by the original laboratory tests on the indicated samples will be reported in Part 1 of this paper, while in Part 2, will be found a summary of conditions found in the various laboratories, which accounted in large part for variances found in the original test data.

PART 1

The object of this investigation was to determine if it is possible to reproduce foundry sand tests in different laboratories, with a reasonable degree of accuracy.

Procedure—It was agreed by the committee members that the first laboratory test on the reproducibility of foundry sand tests should be made on a "standard" molding sand sent pretempered, and sealed in an air-tight container, to each committee member to be tested in his own laboratory. Complete instructions and data sheets were sent with these samples. A sheet for reporting on equipment used was included.

The sand to be tested was a synthetic sand made

up from 96 per cent Northern Indiana dune sand and 4 per cent western bentonite, by weight, with approximately 2 per cent moisture added. This mix was carefully weighed out and mulled. Immediately after mulling, the entire batch was sealed in gallon cans and shipped express to the several committee members. All tests were made on the same date.

A compilation of the average test results reported is reproduced in Table 1 of this paper. These data have also been arranged in the form of frequency charts in Fig. 1. It can be noted from Fig. 1 that there is a considerable spread in the values obtained for each test. The green permeability as measured by the orifice method shows the best distribution of all tests considered.

Further information regarding variations obtained is given in Table 2 which gives the percentage of tests exceeding plus or minus 15 per cent of the grand average and also the number of laboratories reporting values outside these limits. Only in the permeability tests do the reported values generally remain within the limits of plus or minus 15 per cent of the average. Poor results were obtained in the tests on the dry sand.

Moisture Tests

Most of the deviations from the average in the moisture test seemed to be on the plus side. Reference to Fig. 1 indicates this condition. There is greater scatter in the standard moisture test than in the rapid test.

Possible sources of error in this test are as follows:

- 1. Inaccurate weights
- 2. Lack of sensitivity in balances
- 3. Variation in room humidity
- 4. Gain in moisture content before original weighing
 - 5. Loss in moisture content before original weighing
 - 6. Failure to evaporate all moisture during drying
- Condensation of water vapor in test can on walls of can with concurrent loss of moisture in sand sample
 - 8. Leak in screen in rapid drier (in old style pan)
 9. Loss of part of sample after original weighing
 - 10. Moisture adsorption when weighing dry sample. The gain in moisture before weighing could be

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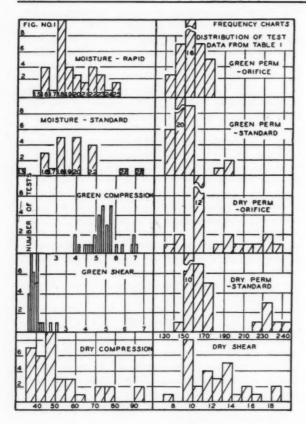
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^{*} Foundry Engineer, Carpenter Brothers, Inc., ** Associate Professor, Dept. of Metallurgical Engineering, University of Wisconsin and *** President, Harry W. Dietert Co., Detroit.

TABLE 1-REPRODUCIBILITY OF FOUNDRY SAND TESTS A.F.S. Green Sand Properties Committee Average Test Results

| Property | A | В | C | D | E | F | G | Н | 1 | J | K | L | M | | er Cent |
|---------------------------|--------|-------|---------|-----------|-----------|-----------|-------|----------|---------|--------|------|-------|------|--------|---------|
| l Moisture (rapid method) | | 1.6 | 2.3 | 1.87 | 2.23 | 1.81 | 1.7 | 2.26 | 1.78 | 2.0 | | 2.4 | 1.58 | 1.96 | 41.8 |
| 2 Moisture (oven method) | 1.97 | 1.8 | 2.2 | 1.8 | 2.0 | 1.75 | | 2.2 | 1.9 | | 1.9 | 2.8 | 1.5 | 1.98 | 65.6 |
| 3 Green Perm (orifice) | 153 | 169 | 148 | 156 | 140 | 138 | 172 | 150 | 146 | 152 | 161 | 157 | 174 | 155.0 | 23.5 |
| 4 Green Perm (std) | 137 | 153 | 134 | 153 | 147 | 142 | 153 | 150.4 | 149 | 149 | 145 | 139 | 188 | 149.0 | 36.5 |
| 5 Green Compression | 5.46 | 5.7 | 4.8 | 6.1 | 4.07 | 5.28 | 5.7 | 5.0 | 5.8 | 5.3 | 5.3 | 5.0 | 7.0 | 5.42 | 54.0 |
| 6 Green Shear | 1.6 | 2.5 | 1.3 | 1.8 | 1.4 | 1.38 | 1.4 | 1.28 | 1.4 | 1.6 | 1.3 | 1.55 | 1.43 | 1.53 | 79.8 |
| 7 Deformation | 0.0056 | | 0.010 | 0.0055 | | 0.016 | | 0.012 | | | | 0.014 | | 0.0105 | 100.0 |
| 8 Toughness | 31.0 | | 48. | 33.6 | 48.8 | 85.5 | | 62.2 | | 72 | | 70. | | 56.4 | 96.7 |
| 9 Dry Perm (orifice) | | 197 | | 237 | 230 | 163 | 170 | 165. | 148 | 161 | 175 | 178 | 141 | 178.7 | 53.9 |
| 10 Dry Perm (standard) | | 174 | | 238 | 236 | 166 | 164 | 155.5 | 156 | 155 | | 164 | 155 | 176.3 | 47.1 |
| 11 Dry Compression | 41.5 | 51. | 58.5 | 47.0 | 56 | 38.6 | 44.5 | 47.8 | 38 | 75 | 44.5 | 37. | 87 | 50.5 | 99.0 |
| 12 Dry Shear | 9.3 | 13.5 | 14.6 | 10.3 | 17.5 | 13.1 | 46.1 | 12.2 | 9.8 | 11.4 | 9.9 | 57. | 35.3 | 12.2 | 67.3 |
| 13 Wt. of specimen | | 150 | 156 | 149 | 160 | 153 | 155 | 151 | 153 | 155 | | 155 | | 153.7 | 7.2 |
| | | Total | percent | age sprea | d of hig | h and l | ow re | sults fr | om av | erage. | | | | | |
| | | R | eportin | g labora | tories re | ferred to | abov | e by le | etters. | | | | | | |



obtained by weighing or exposing the sand in an atmosphere of high humidity. That such a condition might be realized is borne out by the curve plotted in Fig. 2, which shows a general relationship between atmospheric humidity and the moisture content of the test sample.

Green Permeability

In the standard test, a number of the reported values were higher than the average, otherwise the tests checked quite closely. The possible sources of error for this test were as follows:

- 1. Variation in intensity of ramming
- 2. Leak in permeability meter

TABLE 2-SUMMARY OF TEST ACCURACY

| Test | Average | | | | No. of Labs. Report- ing | No. of Labs. with 1 or more tests outside Limits |
|---------------------|---------|------|------|-----|-----------------------------------|--|
| Moisture-rapid | 1.95 | 1.66 | 2.24 | 69 | 13 | 8 |
| Moisture-oven | 1.98 | 1.68 | 2.28 | 74 | 10 | 3 |
| Green Perm-orifice | 155 | 132 | 178 | 100 | 13 | 0 |
| Green Perm-standard | 1 149 | 127 | 171 | 92 | 13 | 1 |
| Green Compression | 5.42 | 4.63 | 6.26 | 78 | 13 | 4 |
| Green Shear | 1.53 | 1.27 | 1.73 | 76 | 13 | 6 |
| Dry Perm-orifice | 179 | 152 | 206 | 66 | 11 | 4 |
| Dry Perm-Standard | 177 | 150 | 204 | 66 | 10 | 3 |
| Dry Compression | 51.0 | 43.4 | 58.6 | 43 | 13 | 8 |
| Dry Shear | 12.2 | 10.5 | 13.9 | 41 | 10 | 8 |

- 3. Inaccurate orifices
- 4. Type of specimen tube
- 5. Moisture or mercury in the air tube
- 6. Scratches on the specimen tube
- 7. Sand grains in mercury seal causing leakage
- 8. Inaccurate pressure gage
- 9. Variation in temperature and barometric pressure

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10. Moisture content variations in sample.

Inasmuch as there was good agreement in the rapid test and a few high values in the standard test, it might be argued that the latter resulted from conditions contributing to leakage in the air system. By requiring a longer time to complete, the standard test would be more subject to these sources of error than the orifice method.

Dry Permeability

In addition to the possible sources of error listed for the green permeability test, the following can be added for the dry permeability test:

- 1. Specimen not dried at correct temperature
- 2. Specimen not correctly sealed in tube.

The latter source of error seems most likely and this is supported by the evidence which shows considerable scatter on the plus side of the average results.

Green Compression

Deviations from the average were about equally

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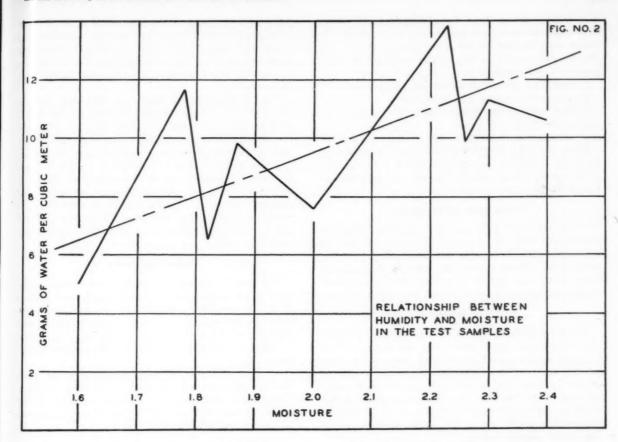
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distributed on both sides of the average, although a number of test values on the plus side deviated by a considerably greater percentage than those on the minus side. Possible sources of error include the following:

1. Variation and intensity of ramming

2. Type of specimen tube

3. Humidity and moisture

4. Size and weight of specimen

- 5. Rate of travel of compression head on machine
- 6. Preparation of sand prior to ramming
- 7. Reading wrong scale on testing machine
- 8. Mathematical errors
- 9. Friction in testing machine
- 10. Pendulum riding on bumper.

With two or three exceptions, the individual tests at the separate laboratories checked each other quite closely. Thus, the tests seems quite reproducible when made at any one of the laboratories; but there is not such good agreement between individual laboratories.

If the green compression test values are tabulated against the moisture content, the results in Table 3 are obtained. It is seen that there is an apparent trend toward higher moisture contents in the sands of lower green strength. If this trend is real, it accounts for some of the discrepancies in the test results. Furthermore, it would indicate that some of the sands ran higher in moisture content than others. This might be attributed to the way the individual samples were selected and placed in gallon cans when the original

TABLE 3—COMPARISON OF GREEN COMPRESSIVE STRENGTH VALUES WITH THE MOISTURE CONTENT

| Green Compressive Strength, psi | Moisture Content, |
|------------------------------------|-------------------|
| 7.0 | 1.5 |
| 6.1 | 1.8 |
| 5.8 | 1.9 |
| 5.7 | 1.8 |
| 5.7 | 1.7 |
| 5.46 | 1.97 |
| 5.3 | 2.0 |
| 5.3 | 1.9 |
| 5.28 | 1.75 |
| 5.0 | 2.2 |
| 5.0 | 2.8 |
| 4.8 | 2.2 |

sand sample was mulled, but the curve in Fig. 2 suggests rather that the moisture may have been absorbed during the tests. In either case, the data indicate that, if the moisture content variations are real, they in turn could account for variations in other properties as well.

Green Shear

As in the compression test, the greatest deviations from the average occurred on the plus side. The same sources of error can be listed for this test as were given for the green compression test.

Dry Compression and Dry Shear

Such wide deviations occurred in these tests that

it is difficult to see how the results from dry sand tests could be used with much confidence unless sufficient tests are run to reveal the general trend. To the possible sources of error listed under green compression can be added:

1. Specimen not dried at correct temperature

2. Specimen not cooled in dessicator

3. Specimen losing sharp edges and true shape

during handling

4. "Brittleness" of sample leads to scatter in results. Since the dried samples would not be expected to have the plasticity of green samples, unequal distribution of the stresses during test may possibly account for the larger scatter.

Deformation and Toughness

It appears that some operators divided their deformation results by two whereas others did not. If this supposition is correct, the data indicate a relatively close check among the various deformation tests. Toughness values, of course, would not check if the deformation values differed by a factor or two.

Conclusions

These preliminary tests show that, with the possible exception of the permeability test, the common tests on green molding sand are far from foolproof in their present state of development. Even the relatively simple moisture test is subject to considerable error. In view of the fact that these tests were made under more or less controlled conditions, it is of interest to speculate on the errors that might be encountered in normal routine testing.

No attempt has been made in this report to present a complete analyses of the data, but the intention has been to collect the test results and to comment on those points which were most obvious. It is hoped that others will be able to supply additional comments, based on their practical experience, which will aid in interpreting the data and in indicating the

direction for future investigations.

PART 2

Since the test figures reported by the various laboratories in Part 1 did not check within reasonable limits, it was decided that an investigation should be made at each laboratory to determine, if possible, the reasons for the widely varying results obtained. In the following paragraphs will be found a discussion of the conditions observed at the ten laboratories that were visited, and some comments as to how they affect the uniformity and accuracy of the foundry sand

It has been seen that comparisons of sand test values from a group of laboratories do not always show close agreement. Many factors, such as sampling, accuracy of test, standardization of test procedure, maintenance of test equipment, and the human equation enter into the picture to cause variation in test results.

Ten different sand testing laboratories were visited by two of the committee members, testing two different types of molding sands for moisture, permeability and green compressive strength. The sand referred to hereafter as No. 1 consisted of 96 per cent Michigan lake sand, 4 per cent western bentonite and 2 per cent water. The sand was passed through a quarter-inch riddle after mixing in a No. 1 Simpson mixer. The sand was then placed in one gallon cans and sealed.

The second type of sand was obtained from a foundry heap which was mixed by a tractor type B & P. sand slinger. A pile of this sand which had passed through the head was riddled through a one-quarterinch screen and placed in one gallon cans and sealed.

A can of each sand was delivered by car to each of nine laboratories on nine consecutive days. A period of two weeks elapsed between the ninth and the ten laboratory. Inspection sheets were used to record the calibration and state of maintenance of the testing equipment at each laboratory before any tests were made.

Testing Equipment

The state of maintenance of the sand testing equipment at the majority of the laboratories was found to be in poor condition. Insufficient time was devoted to keeping the equipment clean, in good repair and lubrication. Little time apparently was ever given to check or make minor adjustments on the equip-

A detail of the state of maintenance of the sand testing equipment may be had by studying Table 4. Table 4 is a tabulation of the inspection sheets giving pertinent data on the condition of testing equipment at the various laboratories.

Comparative Test Results

Test data secured at each of the ten laboratories are shown in Table 5. Three or more test readings were made of sands No. 1 and 2 at each laboratory. In addition to the tests on the sands No. 1 and 2, three master permeability tubes were used to obtain permeability readings at each laboratory. These master permeability tubes were made by having a waterproof resin bonded core baked in a specimen tube. Each tube had a core made with sand of different fineness. No test readings were discarded from any series of readings for a given test.

A comparison of the figures shown in Table 5 with those contained in the original committee report will show that a much greater degree of reproducibility has been achieved, largely because testing equipment found to be in poor condition and not calibrated correctly was adjusted and put in good working order

before these tests were made.

As found in Part 1, it will be noted that in this series of tests the moisture determination made by the rapid method showed a slightly greater accuracy than moisture determinations made by the oven method. This is not what one would expect. It may be accounted for by less human equation entering into the rapid moisture tests.

Test on Sand Rammer

The sand rammer at each of the laboratories was placed in as good repair as possible under the time available for the tour and facilities at hand. When

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Table 4-Calibration-Maintenance of Equipment

| Lab. | Balance | Weights | Moisture Teller |
|------|---|---|--|
| A | No balance used. | No weights used. | Grubb Moisture Teller. Compressed air. In good working order. Checked calibration. |
| В | No. 250. Center bearing cover rough. Replaced with new cover. | Used analytical weights. | No. 270. Funnel pushed to one side Straightened, to give good fit with pan. |
| С | No. 250-fair repair. Removed rough center cover. 20 mg at 200 gr. | All weights tend to the heavy side. | No. 270-in good repair. Some looseness in pans. Tightened same. |
| D | No. 250-fair repair. 20 mg at 200 gr. | Old wts. Not very accurate. Used Dietert analytical weights. | No. 270. Holder required lubrication. Tightened funnel screws. Oiled motor bearings. |
| E | No. 250—good repair. | Wts. of hand badly worn. Used Dietert analytical weights. | No. 270—good condition. Lubricated unit. |
| 1 | Torsion. Good repair. 20 mg at 200 gr. Used Dietert No. 250 balance since this was the type balance used on other tests. | Sliding weights. Good accuracy except 2 gr slide. Used Dietert weights, since other weights were in use. | No. 270 with 1/2-in. deep pans. Good repair. |
| · K | No balance normally used. Torsion available. | No weights. Used Dietert weights. | Speedy Moisture Tester. |
| L | Cenco lecture type. New. 50 mg sensitivity at 200 gr. | All weights heavy. Did not use them in test. Used Dietert analytical weights. | No. 270. Tightened screens. Pans heavy. Cleaned and balanced. |
| M | Triple beam balance. Not used in these tests. Pan too small. Used Dietert balance. | No good weights available. Used Dietert analytical weights. | No. 270. Good repair. Balanced pans and lubricated. |
| 0 | No. 250 Lecture type—good condition. 20 mg sensitivity at 200 gr. | 20 to 200 grgood; 10 grlight; 0.2 to 5 gr heavy; 1 gr light | No. 275 Heavy Duty. New. No change made. |
| Lab. | Rammer | Permeability | Strength |
| A | Old rammed badly worn. Installed new rammer. Impact ring 0.535 in. | Good repair. No changes made. | Motor-driven. Good repair. Rate of loading 27.2 psi per min. 3% error at 5 psi, 1% error at 12 psi. |
| В | Sand rammer new. Bolted to concrete pillar. Changed graphite lubrication to oil acct. poor impact specimen. Im- pact ring 0.521 in. | Good repair. Zero setting OK. Calibrated orifices for barometric pressure and std. temp. | Motor-driven. Zero setting OK. Rate of loading 25.8 psi per min. No calibration made. |
| С | New No. 310. Bolted to 60 lb. cast iron base on wood timber. 2.000-in. drop. Impact ring 0.529 in. | New. No changes made. | Motor driven. Good repair. Loading rate 25.2 psi per min. 1% error at 5 psi _h 0.5% error at 12 psi. |
| D | Bolted to concrete pillar. Good repair except very dry. Lubricated. 2.000-in. drop. Impact ring 0.537 in. | Dirty mercury cup. Leaked too much sand. Cleaned, then OK. Adjusted ori- fices to barometer pressure and stand- ard temperature. | Motor driven. Good repair. Oiled. Rate of loading 25.6 psi per min. 3% error at 5 psi, 1% error at 12 psi. |
| E | Installed new rammer. Old one worn out. New rammer bolted to 60 lb. C.I. blcck. Set unit on concrete floor. Wood pedestal too loose. Impact ring 0.553 in. | Good repair. Cleaned orifices with wood reamer. | Motor driven. Not level. Rate of loading 32 psi per min. 1% error at 5 psi, 11% error at 12 psi. |
| 1 | In poor repair. Cam and crosshead worn. Wt. drop 1.965 in. Repaired to give 2.000-in. drop. Bolted on side to | Good repair. Orifices adjusted for barometric pressure and temp. | Motor driven. Good repair. Loading rate 25.4 psi per min. 1% error at 5 psi, 0% error at 12 psi. |
| K | 2-in. pine top. Impact ring 0.553 in. New cam found on rammer ground to give 2.0-in. drop. Rammer fastened to top of wooden bench covered with springy steel. Impact ring 0.588 in. | Cleaned orifices. Zero setting off. Good repair otherwise. | Motor driven. Wt. shifted on fulcram shaft. Zero off. Not level. 1% error at 5 psi, 0% error at 12 psi (after adjustments) |
| L | In poor repair. Cam worn. Gave 1.959- in. drop. Repaired to give 2.000 in. Rammer bolted to 2-in. table top with 3x3-in. legs. Impact ring 0.565 in. | Good condition. Changed orifices for temp, and barometric pressure correction. | Hand operated. Was turning too fast. 4% error at 5 psi, 2% error at 12 psi. |
| M | Bolted to wood timber resting on concrete floor. Good repair. Impact ring 0.556 in. | In good repair. Adjusted orifices for barometric pressure and temp. | Motor driven. Good repair. Loading rate 25.4 psi per min. 0% error at 5 psi., 0.8% error at 12 psi. |
| 0 | New No. 310 Rammer bolted to cast iron base. Wt. drop 2.005 in. No lubri- cation. Impact ring 0.532 in. | Good. No changes made. 0% error at 1000 cc's, 10% error at 2000 cc's. | Motor driven. Good repair. Loading rate 32 in. per min. 2% error at 5 psi. 1.26% error at 12 psi. |

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TABLE 5-SAND TEST DATA FROM 10 LABORATORIES

| | | Sa | nd N | 0. 1 | Mich | igan l | Lake S | and - | + 4% | Ben | toni | te | Sand | No. | 2 Fo | undry | Hear | Sar | nd 1 | Mast | er Po | erm. | Tub | es |
|-------------------|----------|-----------|---------|--------------------|------|----------|------------------|-----------------------------------|---------------------------------|---------------|------------------|---------------------|--------------------|------|---------------------------------|---------------|------------------|---------------------|---------------|------------------|---------------|------------------|---------------|------------------|
| | | | N | % foist | | | Sand Samme | | e | Per | | M | % loistu | re | | Perr | | | 140- | -3 | 70- | 5 | 20 | -5 |
| Laboratories | Humidity | Barometer | °C-Temp | Moisture Teller | Oven | Mounting | Specimen Tube | Impact Specimen Height, in. | Specimen Density Ib/cu ft | Std. Perm. | Orifice Perm. | Green Comp. Psi. | Moisture Teller | Oven | Specimen Density lb/cu ft | Std. Perm. | Orifice Perm. | Green Comp. Psi. | Std. Perm. | Orifice Perm. | Std. Perm. | Orifice Perm. | S:d. Perm. | Orifice Perm. |
| 0 | 37.5 | 750 | 25 | 2.06 | 2.26 | Good | Poor | 0.532 | 130.8 | 140 | 150 | 5.0 | 5.03 | 5.4 | 119.0 | 130 | 119 | 8.6 | 213 | 230 | 85 | 87 | 24.0 | 24.0 |
| C | 17 | 755 | 25 | 1.98 | 1.98 | Good | Fair | 0.530 | 129.5 | 141 | 150 | 5.3 | 5.26 | 5.18 | 119.6 | 114.9 | 122 | 9.2 | 223 | 243 | 89 | 88 | 22.8 | 22.0 |
| D | 15 | 740 | 22.4 | 2.13 | 2.2 | Good | Good | 0.537 | 130.0 | 134 | 142 | 5.0 | 5.14 | 5.3 | 120.5 | 119 | 120 | 9.4 | 219 | 235 | 85 | 85 | 23.5 | 23.7 |
| E | 13 | 732 | 23 | 2.16 | _ | Good | Fair | | 132.0 | 138 | 137 | 5.0 | 5.13 | _ | 120.3 | 141 | 135 | 9.28 | 223 | 212 | 87 | 82 | 29.8 | 28 |
| В | _ | 747 | 23 | 2.0 | _ | Good | Fair | 0.521 | 131.4 | 137 | 151 | 5.0 | 5.0 | - | 120.5 | 108 | 120 | 8.9 | 215 | 243 | 85 | 83 | _ | - |
| L | 26 | 736 | 26 | 1.9 | 2.0 | Fair | Fair | 0.565 | 130.5 | 138 | 155 | 5.1 | 5.1 | 5.0 | 120.0 | 111 | 128 | 8.6 | 210 | 240 | 82 | 91 | 22.7 | 23 |
| I | 15 | 756 | 20 | 1.9 | 2.0 | Fair | Fair | 0.553 | 127.9 | 140 | 148 | 5.2 | 5.07 | 5.1 | 119.6 | 114.9 | 122 | 9.2 | 223 | 243 | 89 | 88 | 22.8 | 22 |
| M | 11 | 753 | 19.5 | 2.1 | 2.1 | Fair | Fair | 0.529 | 129.0 | 147 | 151 | 5.1 | 5.2 | 5.1 | 117.2 | 117.9 | 117 | 8.05 | 210 | 219 | 82.6 | 77 | 22.4 | 21 |
| A | 19.5 | 756 | 26.6 | 2.1 | 2.1 | | | 0.534 | 128.5 | 140 | 151 | 5.0 | 5.0 | 5.2 | 120.3 | 112 | 109 | 8.6 | 210 | 243 | 86.7 | 88 | 23.3 | 22.7 |
| K | | | 5 | peed | V | | | | | | | | Speed | ly | | | | | | | | | | |
| | _ | 735 | | | | Poor | Fair | 0.586 | 131.4 | 137 | 151 | 5.0 | 5.0 | _ | 119.0 | 119 | 107 | 7.0 | 218 | 212 | 84 | 82 | 24.0 | 21 |
| Grand | | | | | | | | | | | | | | | | | | | | | | | | |
| Average | 19.2 | 716 | 23.5 | 2.03 | 2.09 | | | 0.543 | 130.1 | 139 | 148 | 5.07 | 5.09 | 5.18 | 119.6 | 118.8 | 119.8 | 8.7 | 216 | 232 | 85.5 | 86.1 | 24.0 | 23.1 |
| Coefficient of | | | | | 4.8 | | | 3.6 | 1.0 | 2.2 | 3.3 | 2 | 1.7 | 1.6 | .8% | 7.8 | 6.5 | 7.5 | 5.3 | 5.3 | 2.7 | 5.2 | 9.2 | 8.6% |
| Variation | | | | | | | | | | | | | | | 7.0 | | | | | | | | | , |
| % | | | | 6.4 | 8.1 | | | 7.8 | 1.4 | 5.7 | 4.7 | 4.5 | 3.3 | 4.2 | .8 | 18 | 12.7 | 8.0 | 3.3 | 4.7 | 4.0 | 8.0 | 24.1 | 21.2 |
| Variation + | | | | | | | | | | | | | | | | | | | | | | | | |
| From Average — | | | | 6.4 | 5.2 | | | 4.0 | 1.2 | 3.0 | 7.4 | 1.3 | 1.7 | 5.4 | 2.0 | 9.0 | 10.7 | 19.5 | 2.7 | 8.6 | 4.0 | 10.5 | 6.6 | 9.0 |

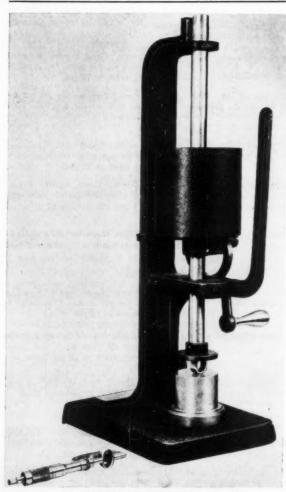


Fig. 3-Master Impact Tester

the sand rammer at laboratory "B" was inspected it was found to be lubricated with foundry graphite. This rammer gave erratic impact test results. The graphite was removed and moving parts oiled. Under this condition, the rammer gave consistent results. This revealed how important the lubrication of the rammer really was. The test readings obtained in this tour would have been much better had we had this knowledge at the beginning.

The impact delivered by the rammer weight was determined by placing a 0.750 in. on by 0.065 in. ID brass tube, 0.500 in. in length on top of a 2 in. dia pedestal held centrally within the cup pedestal of the rammer. The rammer plunger rests on top of this brass ring. The ring, as shown in Fig. 3, received the impact from three rammer weight blows. The diameter of the impacted ring specimen is recorded, as the impact energy. The average diameters from two rings are tabulated in Table 5. The readings are reproducible when the rammer is in good working order, mounted solidly and well lubricated. The variation in reading of this impact specimen is from 0.521 in., found in laboratory "B," to 0.586 in. for rammer at laboratory "K." At this latter laboratory the rammer was mounted on a table which was covered with a springy steel plate which absorbed some of the ramming energy. The rammer at laboratory "B" with the 0.521-in. impact specimen was securely mounted on a concrete pillar. The variation of rammer mounting found on this tour explains in part why laboratory test results do not agree precisely.

Specimen Tubes

In the past it was recognized that smoothness of the specimen tube bore did effect permeability and strength readings. However, not until this tour when a sand which was sensitive to ramming of specimen tube bore was used, was it realized how great the variation in strength readings could be with different specimen tube surfaces.

Sand No. 2 is a sand which is of the sensitive type

and No. 1 is of the insensitive type.

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The study of the effect of specimen tube surface smoothness on green compressive strength readings was undertaken after this tour. A group of specimen tubes of different smoothness and surface treatment were made. The smoothness of four tube bores were measured in rms (root mean square). Referring to Table 6, it may be noted that as the rms decreases, the strength reading increases rapidly for the sensitive sand No. 2. A group of laboratories equipped with specimen tubes with a uniform surface finish could secure permeability and strength readings of much greater accuracy than heretofore possible.

TABLE 6-SPECIMEN TUBE BORE AFFECTS
STRENGTH READINGS

| Specimen Tube No. | 1 | 2 | 3 | 4 | 5 | 6 | 7 |
|----------------------|----------------|-------------------|-------|----------|--------|--------|---------|
| Description | Used Ground | Ground Blacked | | d Honed | Honed | Honed | Honed |
| | | | | | | | 21/2 to |
| RMS | _ | _ | _ | 11 to 15 | 7 to 9 | 4 to 6 | 31/2 |
| ID Max | 2.0035 | 2.002 | 2.001 | 2.001 | 2.0005 | 2.001 | _ |
| ID Min | 2.0035 | 2.001 | 2.000 | 2.001 | 2.0005 | 2.001 | _ |
| Avg. Density | 100.3 | 99.4 | 100.6 | 100.5 | 100.6 | 100.7 | 100.8 |
| Av. Perm | 117. | 127. | 115. | 116. | 117.5 | 114.8 | 114.8 |
| Sand, Wt., gn | 165. | 165. | 165. | 165. | 165. | 165. | 165. |
| Avg. Strength | | | | | | | |
| psi | 9.24 | 7.66 | 9.8 | 9.58 | 9 54 | 9.88 | 10.38 |

Specimen Density

The density expressed in pounds per cubic foot of the A.F.S. standard 2 x 2 in. sand specimen was calculated for each laboratory. The density readings did not reveal any particular fact concerning the efficiency of ramming and how other test readings were affected.

Permeability

The permeability readings were taken by the standard stop-watch method and the orifice method. The study of the variations in permeability readings shows that in the case of the No. 1 sand, master permeability tube 140-3 and 70-5, the standard method showed less variation while the orifice method had less variation for the No. 2 sand. As for the accuracy of reading, the standard permeability method certainly should be favored.

Green Strength

An exceptionally good series of strength readings were obtained in the case of the No. 1 sand. The coefficient of variation is 2 per cent. The maximum reading obtained was plus 4.5 per cent, while the minimum was minus 1.3 per cent from the average. The greater variation was obtained in the case of the No. 2 sand. The coefficient of variation is 7.5 per cent with the maximum reading 8 per cent above the average and the minimum reading 19.5 per cent below the average, which was obtained at laboratory "K" where the rammer was cushioned on the sheet steel.

The strength reading could have been improved in accuracy if the reversing trigger rod of motor switch had been adjusted. This inspection point was neglected. It is recommended that this rod be adjusted so that a clearance of $\frac{3}{16}$ -in. be obtained between the end of trigger rod and pendulum weight. The standardization of specimen tube surface and sand rammer mounting will also do much to bring strength readings to a higher degree of accuracy.

As mentioned previously, this tour of sand testing laboratories developed the fact that one of the major reasons for the lack of reproducibility of sand tests from different laboratories was the fact that the testing equipment used was not being properly maintained. In the following paragraphs are mentioned some of the more important points regarding each piece of equipment which should be checked periodically if consistent and reliable test data is desired.

Balance

When the lecture type of balance is used, the bearings must be provided with covers to shield the bearings from dirt. Occasionally clean the bearings with a soft cloth. The side plates on the center bearing must be kept smooth. It is recommended that bearing side plates be replaced at proper intervals in order to insure a good sensitivity, for example, 20 mg at 200 grams load. Always weigh with the balance swinging. Make certain that the balance is always in balance, swinging an equal number of divisions each way of center. For the best results, use two balances—the older one for weighing sand samples and the newer balance for the more exact weighing, such as, moisture determination.

Balance Weights

Sand testing laboratories should be equipped with two sets of weights: One set for general weighing and the other for the more accurate weighing, for example, moisture determinations, fineness, and density tests. Weights lose their accuracy in daily routine use.

Rapid Moisture Test

Drying pans used in this test require attention. Keep the pans free of loose dust by blowing air with the mouth against the bottom pan once per day. If the old style pans are being used, it will be necessary to inspect pans to determine that the compression ring is holding filter cloth tight against the bottom of the pan. The next point of inspection is to place the gross weight (pan weight plus 50 grams) on the left-hand pan of the balance and place one of the drying pans on the right-hand side of the balance. Place an accurate 50-gram weight in the pan and weigh. Pans are usually heavy and should this be the case, take a sheet of emery paper and lay it on top of the table and place the pan upside down on the emery paper and with a rotary swinging motion remove some of the metal from drying pan until it balances. All pans should be balanced at least every three months.

Speedy Moisture Tester

Take great care in keeping the inside container of this unit clean. The rubber gasket of the unit must be kept in good repair. Watch for leaks around the gage and possible leaks through the sample cup. Discard the first reading in case the instrument has not been in use for sometime. Frequently check the moisture readings of the instrument against a gravimetric method such as the rapid moisture test, to determine whether the pressure gage is in calibration.

Sand Rammer

The sand rammer has definitely received insufficient attention by all. This unit must be bolted solidly to the top of a concrete, solid metal, or solid timber column. Unless the sand rammer is so mounted, do not attempt to check permeability or strength values with other laboratories. The sand rammer must be kept well lubricated with SAE No. 10 oil.

The cam of this sand rammer requires particular attention. Keep cam bearing and cam surface oiled. Hold cam to the maximum height position and measure the height to which the weight is raised from the crosshead. Proper height is 2 in., plus or minus 0.005 in. The bottom of the rammer plunger and pedestal base surface should be wiped free of sand before using rammer for each specimen. Rotate the specimen tube one-quarter turn as soon as plunger of rammer comes to rest on top of the sand in the specimen tube. Care must be exercised in turning the cam crank. Make three separate and distinct turns of the crank, starting and stopping at the four o'clock position each time. Make a fairly rapid and continuous turn each time.

Specimen Tube

Smoothness of the specimen tube bore greatly affects the strength readings of a sand. The permeability is not so greatly affected. Some sands are more sensitive than others to the smoothness of the specimen tube bore. Slight grooves which run parallel with the axis of the specimen tube are not too serious. For accurate control or research work, one specimen tube should be set aside as a master and used only to check strength readings from specimens made in a regularly used tube against those made in the master tube. When a difference of 5 per cent is noted, a new specimen tube should be used.

For research tests, it is beneficial to mark one end of a specimen tube with tape and place the rammed specimen in this marked end each time. This greatly adds to the reproducibility of the test, since the two ends of a tube may not be of the same smoothness due to wear.

Permeability

The permeability meter should receive more attention to keep it in good operating condition. For example, remove the drum from the meter every four months and wipe the outer surface of center tube in the tank free of grit. Apply a coat of vaseline. Use a cloth and a small rod to clean inside of drum tube. Check the distance of the center tube end from the drum shell and place tube so that it is in the center of the drum.

Water level in permeability tank should be at the water level mark. Place the drum in the permeability

meter tank and allow drum to travel down. It should run free and not touch the tank.

It is also necessary to clean the mercury in the mercury sealed cup with a damp cloth to remove dust and oxides from the mercury. This prevents leakage of air around the wall of the wall of the specimen tube. The tip of the permeability dial must be level with the water meniscus in the manometer tube.

Close one end of the specimen tube with a rubber stopper and set the tube in mercury seal. Open air valve and note whether the drum drops. It should remain in position to indicate no leakage. With orifice removed and with the closed end specimen tube in mercury cup check the pressure reading. It should read 10 cm.

Orifices should be checked approximately every six months. It is important that those who use orifices for control tests should run the standard stop-watch permeability test frequently so as to know how close the orifices read to the standard method. Never use the orifice test for research on new sand acceptance tests unless the orifice reading checks standard permeability.

Universal Sand Strength Machines

The rack gear of the strength machine must be run dry to avoid sand sticking to the gear. Use a small paint brush to remove all the sand from the strength machine, test position, and top of the gear quadrant after each test. Check alignment of the lower test position holes by pushing a %10-in. diam reamer or rod through the lower hole in the pendulum weight. Hold the pendulum weights so that 23%1-in. space is between the machine surface of the pusher arm and the pendulum weight. The next point to check is to level the unit so that a zero reading is obtained on the strength scale.

Personnel

Foundry sands generally cost the foundryman more money in the way of lost production, scrap castings, and inferior castings than any other item in the foundry. When this being the fact, the foundryman should place the better trained personnel on sand control. He should also assign one of the better locations in the shop to the sand control department. Sand behavior and its relation to production, casting defects and casting quality is complex. This requires that the sand control department should not be assigned to someone who has another department to attract his attention. The position of the sand technician must be given greater recognition in the foundry. This may be accomplished by giving to the sand technician equal pay with other high positions in the foundry, good working relationship with other department heads, good working space, and an opportunity to advance in his chosen field with detailed responsibility and authority.

Conclusions

1. The degree of reproducibility of test readings between the ten laboratories is much better than heretofore reported. Many of the tests reproduced very well. For example, moisture, permeability, and mi de

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strength of the sand No. 1. For a sensitive sand, such as No. 2, the results show a variation greater than normal expectancy.

2. When proper consideration is given to sand rammer mountings, repairs and standardized specimen tubes are used, then much improved results can be expected.

3. Test procedure for each test must be performed in accordance with detailed instructions.

4. Personnel of the sand testing laboratory should receive greater attention.

5. Sand testing equipment should receive adequate attention.

6. Quality and design of sand testing equipment must be improved to keep in step with the technical development of foundry controls.

Acknowledgment

Test work referred to in this paper was carried out by the following members of the A.F.S. Green Sand Properties Committee in their own laboratories:

B. H. Booth, Carpenter Brothers, Inc.

W. Chadwick, Manley Sand Co.

C. H. Cousineau, West Michigan Steel Foundry

H. Gervais, Warden King Ltd.

E. Janke, Nordberg Mfg. Co.

C. Loper, Allis Chalmers Mfg. Co.

R. E. Morey, Naval Research Laboratories

G. A. Pinkul, General Electric Co.

H. Ries, Cornell University

P. C. Rosenthal, University of Wisconsin

N. Schlegel, American Manganese Steel Foundry

H. C. Stone, Belle City Malleable Iron Co.

E. Coleson, Campbell, Wyant & Cannon Foundry Co.

DISCUSSION

Chairman: C. B. JENNI, General Steel Castings Corp., Eddy-

Co-Chairman: J. H. Lowe, Wehr Steel Co., Milwaukee.

JULES HENRY: 1 In his oral presentation, the author showed an illustration of a speedy moisture testing apparatus. Did he experience any variation between the speedy method and the

Mr. BOOTH: I personally did not have much experience with the speedy moisture tester, but we did have some laboratories use speedy moisture testers. The results compared favorably with the rapid method. We found on the first test that the moisture results reported by the rapid method showed a slightly better accuracy than those made by the standard method, which of course is supposed to be more accurate. It is a good piece of equipment and if it is kept in good operating condition should give accurate results.

E. C. ZIRZOW:2 When the sand passes through the screen, how will that affect the moisture test?

MR. BOOTH: The test for moisture is based on the water content of the sand. You put 50 grams of sand in the drier and you dry it. The moisture is evaporated from the sand, which is put back on the scales and weighed, the loss in weight being the moisture content. If you remove something besides moisture, namely fine sand, it will be reported as moisture. So, if you should blow out sand through the screen, that loss added to your actual less of moisture will give you a higher apparent moisture than you actually have.

MR. ZIRZOW: How are you going to get away from the sand coming through the screen?

MR. BOOTH: I understand there are new screens available. H. A. DIETERT: 8 The difficulty arises from the fact that so many operators take the moisture pan, turn it upside down and

rap it against the tabletop or some other hard object. This loosens the compression ring that holds the screen tightly against the frame of the pan and therefore some sand may pass underneath the pressure ring and then pass underneath the filter cloth along the walls. This particular type of pan has been discontinued and now the compression ring is held firmly against the walls of the pan by a ring that is rolled into the wall of the pan and is not demountable as the old one was. With this construction the filter cloth can not be loosened and you do not lose sand through the bottom of the pan. You do not have to be concerned about the screen coming loose any more.

BRUCE BOECKER: 4 I sent our permeability instrument in to have it checked and revamped. When it came back there was a new scale with it. That scale does not seem to check with the old one. I had the old opaque scale on the old type permeability meter and new type transparent scale did not check with the old

Mr. DIETERT: The two scales should check. I would suggest that you purchase a permeability standard and check the permeability meter with the permeability standard. If it does not check ask for a service man to try to locate the difficulty.

MR. BOECKER: Did you find any variance between the old type permeability meter with the orifice as compared with the new type where no orifice is used? Is there any difference in the variable between the old and the new type permeability meters?

MR. BOOTH: We did not actually compare the two meters. However, we did run perm tests by both the orifice and the standard method. On the last tests we ran, the No. 1 sand averaged 148 perm by the orifice method. We had a plus variation of 4.7 and a minus 7.4 by the standard method. The perm averaged 139. It averaged a little less with a plus variation of 5.7 and a minus of 3.5. As far as the degree of accuracy is concerned, the standard perm seemed to be a little more accurate. I do not know why the average perm figure should be 9 points less by the standard method. However, I believe this would be about the same relation you would get with the new type permeability

MR. DIETERT: We find regardless of the manufacturer of the permeability meter, if it is calibrated and maintained according to the standards set by A.F.S. you will get identical readings from the permeability standard. It is not a question as to who made the machine necessarily, but it is a question of maintenance and being certain that the machine has been calibrated correctly. Whether you use the orifice method, the A.F.S. stop watch method, or whether you use the automatic permeter, you get the same reading from a given A.F.S. permeability standard. If you use a rammed sand specimen you will bring in an error of ramming depending on the smoothness of specimen tube, rammer mounting and maintenance of rammer. If you use the new A.F.S. permeability standards, which I feel we should all begin to use, we will then quickly find out whether or not our permeability meter is in good maintenance. In case you can not obtain correct reading ask for a service man and have the maintenance department check the meter.

Mr. Zirzow: Let me sight a case in which we cannot get checks, not only on permeability but on strength. I believe it is in the rammer. I have checked the rammer as fast as the weight and the drop are concerned. We have the cantilever base on both rammers. On checking for the impact, we get quite a variation as to permeability and strength and also get variation in density. Why should that be?

There is only one difference and that is the rammer head. There is a difference of about 1/32 in. in the diameter between the two rammer heads. At least that is the only difference we can detect. I know it is not the permeability meter. We get checks not only on the permeability but also on strength so it must be in the rammer. How do you account for it? It has been oiled properly. I cannot find anything wrong with it except that we do not get checks when two different rammers are used.

MR. BOOTH: You have two rammers, two permeability meters and two different laboratories, and you are trying to check one against the other?

MR. ZIRZOW: That is right.

¹ Metallurgist, Forest City Foundries Co., Cleveland.

⁸ Materials Engineering Dept., John Deere & Co., Moline, Ill. ⁸ President, Harry W. Dietert Co., Detroit.

⁴ Sand Technician, The Minster Machine Co., Minster, Ohio.

Mr. Booth: Have you tried checking those meters with a master perm tube?

Mr. Zirzow: Yes.

MR. BOOTH: Do they check all right?

Mr. Zirzow: Yes, they check.

MR. BOOTH: And the meters are in good condition?

Mr. Zirzow: Absolutely.

MR. BOOTH: Are the specimen tubes in good condition?

Mr. Zirzow: We have new specimen tubes.

Mr. BOOTH: Have you tried changing the tubes from one perm meter to the other?

MR. ZIRZOW: We alternated them.

Mr. BOOTH: New tubes are supposed to be of uniform smoothness but we did find one difference.

Mr. Zirzow: These tubes were purchased within the last three months.

MR. BOOTH: Then the only variable you have found is the diameter of the ramming head.

Mr. ZIRZOW: Yes.

MR. BOOTH: That is one variable that we did not check. When we made those tests we did not check the diameter of the ramming head. We checked the inside of the tubes but we did not check the ramming head.

Mr. DIETERT: We did not measure the plunger head. We have found that head does wear and we should formulate specifications covering the correct diameter of rammer plunger head. Is it molding sand or core sand?

MR. ZIRZOW: Molding sand.

MR. DIETERT: Have you checked the maintenance specifically, item by item, as to the drop and the foundation?

Mr. Zirzow: We checked the drop and the weight of the rammer, not the rammer itself but the rammer weight. I am checking the impact values now.

MR. DIETERT: The impact test ring will show that the foundations are not identical.

MR. ZIRZOW: They should be. They are both cantilever bases. However, there is no doubt but that there is a difference. It was stated that there is a difference in the specimen. If one was more than the other, there must be a difference.

Mr. BOOTH: You will find that on the impact ring.

D. F. SAWTELLE: ⁵ Figure 2 shows variation in moisture content. Will the 2 per cent moisture sand actually pick up more moisture if the can is left open?

MR. BOOTH: Yes, the sand with the 2 per cent moisture and 4 per cent bentonite, which is the mix we used, is on the low side for moisture. You would not operate your sand on that low moisture because it would be too brittle. In a laboratory we use that amount because we get the highest strength out of the sand at that point. Probably the moisture would be 21/2 or 3 per cent in actual practice. These moisture pickups we had were only 0.2, 0.3, or 0.4 per cent maximum. Bentonite, as you know, is a material that absorbs moisture rapidly and although I can-

not back this up, I would say that in the laboratory, if they dumped a can of sand on the bench and the humidity was 80, 90, or even 95 per cent, which it was in some of the laboratories, the bentonite would absorb an appreciable amount of moisture. I cannot back this statement with test data. All I am going by is what is reported and the trend seemed to be that way.

MR. SAWTELLE: It would seem rather improbable to me that a 2 per cent moisture sand would pick up more moisture due to the humidity in the air. I can believe perhaps in the testing, where the humidity is low, than a can, no matter how long or short a time it was open, would perhaps lose a little moisture.

MR. BOOTH: It would give you lower moisture. It is just one of those things that the data showed a trend in that direction and I propose that there should be further investigation.

H. Ries: ⁶ I have listened with considerable interest to this paper and I heartily concur in the recommendations that the Committee can make. I think it is undoubtedly true, what they have brought out. In many cases there is neglect to carry out tests according to instructions and also there is a neglect to see that the apparatus is properly adjusted. I do not think it is even safe to assume that when we first get the apparatus it is in perfect adjustment.

There is one important point that this paper brings out and that is the proper location of the sand testing laboratory. I have visited many sand testing laboratories and I have been horrified to see the conditions under which the tests are made. They sometimes have the testing apparatus right out in the foundry subject to grease and smoke and all kinds of dirt. Sometimes they have it in a separate room right off the foundry but they keep the door open all the time, which is just as bad. I think we should be just as careful about the location of the sand testing laboratory as we are with the location of any other laboratory.

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There is one test which the Committee did not refer to and that is the fineness test. I find it will result in a great difference between different laboratories on the fineness test with the same sand. The sieves are not kept properly clean and a relatively small amount of foreign material in these sieves will sometimes make an appreciable difference.

In making the determination for A.F.S. clay two laboratories may get a very appreciable difference. I may cite a case that happened some years ago. A certain producer ran a fineness test and he reported so much clay in his sand. I ran a fineness test on the same sample and I got 5 or 6 per cent less. We were both supposed to be using distilled water. So I requested that he send me a gallon of his distilled water. This he did and when I used his distilled water I checked exactly with him. Then I sent samples of the water to a laboratory at Cornell University and when they tested his water they found it was on the acid side. That accounted for the difference.

⁵ Metallurgist, Malleable Iron Fittings Co., Branford, Conn.

⁶ Consultant, Ithaca, N. Y.

BANDSAWING IN FOUNDRIES

By Geo. H. Sheppard *

Engineering of Foundry Practice to achieve low cost high production has emerged to an important level. Special attention is being given to all departments to achieve and maintain competitive output.

The purpose of this discussion is to give pertinent factual information on bandsawing of rough castings. The writer will discuss bandsawing ferrous castings, and bandsawing nonferrous castings.

Bandsawing Ferrous Castings

Friction Sawing, What Makes the Saw Cut?—Friction sawing is a process whereby sufficient friction is generated between the rapidly moving bandsaw blade and the work to cause momentary softening of the material directly ahead of the saw, thereby enabling the teeth of the high velocity (3,000 to 15,000 fpm) saw blade to remove the soft metal from the kerf. The temperature required at the point of cut ranges from 1,600 to 2,600 F, approaching that of forging temperature of most alloys. All casting materials falling within this softening range may be friction sawed with economical and practical results.

The most practical approach to the right heat for a certain type casting material is to apply approximately 20 to 40 lb feeding force, then starting with lower saw velocity gradually increase speed of the machine, building up temperature until a satisfactory cutting rate is reached. The saw blade will remove the kerf in proportion to the heat generated. To find the correct saw velocity, it is best to have a machine with an infinite speed range.

Materials Most Adaptable

Many foundries are reluctant to cast nickel and chromium castings for fear of high machining costs. Since friction sawing permits fast cutting rates and low tool cost, this fear is gradually being eliminated. The cutting rate of stainless and high alloy steel castings is just as fast as on iron and malleable castings, ranging from 6 to 150 sq in. per min depending on work thickness.

Production Rates

From a standpoint of production rate, low tool cost and operator fatigue, the maximum work thickness limitation established for practical results is 1 in. On the other hand, if some of the foregoing factors can be sacrificed, sections greater than 1 in. thick may be cut efficiently when using a rocking technique. This technique consists of slightly raising and lowering or rocking the casting as it is fed into the saw. This causes a smaller section of the casting to contact the saw blade thus making it easier to build-up sufficient temperature to permit cutting heavy sections.

Cutting rates applicable to ductile iron, gray iron, chromium and nickel castings are as follows:

| Work Thickness, In. | Cutting Rate sq in./min | | | | | |
|------------------------|-------------------------|--|--|--|--|--|
| Up to 1/8 | 80-150 | | | | | |
| 1/8 to 1/4 | 20-80 | | | | | |
| 1/4 to 1/2 | 10-20 | | | | | |
| 1/2 to 3/4 | 8-10 | | | | | |
| 3/4 to 1 | 6-8 | | | | | |

In addition to the fast production rate and low tool cost factors, friction sawing permits efficient cutting on gating virtually inaccessible with any other trimming method. It also enables cutting stubs, fins, etc. smooth and close enough to the casting so as not to require additional finishing. It is quite practical where the trim-cut must follow the curvature of the casting.

Saw Life

Tool cost can be estimated quite accurately when certain facts are known. The most important factors are: material composition, minimum and maximum work thickness, type of cuts, material hardness and the degree casting has been cleaned. Depending on the conditions stated, saw life ranges from 3 to 24 hr providing the bandsawing machine and saw blade has ample capacity for the work. Friction sawing consumes more horsepower than any other form of band sawing, usually from 5 to 20 hp. The thicker the material the more horsepower required, and it is important the machine have maximum mechanical capa-

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^{*} DoAll Research Laboratory, Des Plaines, Ill.

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Photo shows high speed sawing of gate from aluminum alloy casting.

city and foundry ruggedness. It should be realized that flex life of the saw blade is more or less proportional to wheel diameter, therefore, the larger the wheels the longer the blade life. Rigidity of the machine and balance of driven parts are equally important, as are blade cushioning and proper saw tension.

Saw Type

Carbon type, hard edge flexible back precision friction sawing blades have been found to possess greater abrasion resistance and flex life and retain set longer than spring temper type blades. Saws recommended vary in width from ½ to 1 in. Narrower saws and even round blades are available for specialty applications. The law is to use the widest blade possible, keeping in mind that narrower blades are strictly for radii cutting. In foreign countries, the earlier practice was to employ plain bands with no teeth, but research and practical application has proved that 10 to 18 teeth per inch are more practical for efficient performance.

Work Thickness, Radii and Saw Width

As gates increase in size and the cuts are to be made along the curvature of the castings, it is important to understand certain rules regarding radii, feeding pressure and work thickness. Thicker work sections require greater feeding pressure and to withstand this force, wider blades must be used, thereby limiting the radii that a certain width blade will cut. To clarify this, the following table shows material thickness, minimum saw width according to material thickness and minimum radii that width saw will produce.

| Material Thickness, in. | Minimum Saw Width According to Material Thickness, in. | Minimum Radii, in. | |
|-------------------------------|--|-----------------------|--|
| 1/8 | 1/4 | 5/8-1 | |
| 1/4 | 3/8 | 1-3 | |
| 3/8 | 1/2 | 3-5 | |
| 1/2 | 5/8 | 5-7 | |
| 3/4 | 3/4 | 7-9 | |
| 1 | i | 9-Up | |

Clean Castings Reduce Tool Cost

Before friction sawing, the castings should be cleaned as thoroughly as possible of sand and foreign matter, otherwise the silicon, etc., acts as an abrasive, reducing blade life and resulting in high tool cost.

Finish

Heat generated by the high velocity saw seems to concentrate itself directly ahead of the blade thus minimizing the amount of heat dissipated into the side wall of the finished cut. The depth of heat penetration and hardness is only skin deep, depending greatly on the material being cut and sawing techniques employed. If the cut is made too slow, deeper penetration will result, also having the correct saw velocity is a determining factor in minimizing heat penetration. This skin hardness ranges up to approximately 0.0003 in. deep. The surface of the finished cut varies from a pronounced shallow saw scratch to one scarcely visible to the naked eye. It is characteristic of this process to form a slight burr \(\frac{1}{32} \) to \(\frac{1}{16} \) in. on the underside of the cut, which is thin, and not objectionable.

Special Casting Material and Alloys

New alloys and casting materials are constantly being tested to determine sawability rating. Certain heat resistant or high temperature materials requiring elevated temperature to soften are impractical to cut from a standpoint of friction sawing, but will bandsaw efficiently using conventional low speed sawing, line milling or line grinding techniques. Considerable work has been done on the new titanium-base alloys and while some of these alloys may be friction cut, they require a lower saw velocity than normally used. For example, the usual practice for friction cutting ferrous castings is to employ a saw velocity from 8,000 to 15,000 fpm, whereas due to characteristics of titanium-base alloys, a saw velocity of only 5,000 fpm is required. A higher saw velocity seems to prevent softening of the material and prevents efficient tooth penetration.

Bandsawing Ferrous Castings

Low Speed Sawing, Unlimited Work Thickness—This is a process frequently referred to as contour sawing, utilizing low saw velocities, ranging from 40 to 1,500 fpm. This technique requires conditions whereby the cut must remain cool for maximum performance and production, just the opposite to friction sawing. Low speed sawing permits cutting any thickness of casting with accuracy. It is generally employed on sections greater than 1 in. thick, due to the fact

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that on thinner sections friction sawing permits a faster cutting rate.

What Makes the Saw Cut

Speed of the saw is determined according to the composition of castings, how thoroughly the castings have been cleaned and work thickness. As stated before, it is important to keep the saw teeth as cool as possible at the point of cut, which is especially important from a standpoint of saw life. If detrimental frictional heat is generated at the point of cut, saw tooth sharpness is impaired resulting in high tool cost. The higher the percentage of wear elements present, such as silicon, nickel, etc. and the higher the hardness, the lower the saw velocity must be in order to achieve maximum blade life. The machines best equipped to meet these requirements are the infinite variable speed machines. Prior to this year, machines were available with a limited speed range from 50 to 1,500 fpm, but recently a machine was introduced which has an expanded infinite speed range from 40 to 10,000 fpm making it possible to quickly establish the proper sawing technique for any type casting material. Since we know what the material characteristics are, the procedure to follow for establishing the proper sawing technique is relatively simple.

Best Cutting Technique

Contour sawing machines have infinite speed range. It is a simple matter to select the proper predetermined sawing technique and speed range for any given material.

To gain the greatest benefit, a machine should be equipped with hydraulic feed table especially when heavy castings are involved. Feeding and holding jigs are beneficial, and will more than pay for themselves in increased cutting efficiency and production per man hour.

Finish—Since low speed sawing employs precision type sawing tools, it is reasonable to expect accuracy to plus or minus 0.003 to 0.005 in. The cuts may be made following the exact curvature or outline of the casting thereby eliminating the necessity of further machining or grinding. The condition of finished cut is burrless, free from diagonal, horizontal or vertical harmonic ripples, and has only a slight scratch, which is the result of and related to the number of teeth per inch of saw blade. Due to the cool cutting action of a properly equipped saw, it is possible to achieve long saw life, warp-free cuts, excellent surface finish and substantial production.

Cutting Rate—With the evolution of sawing, it is now possible to study the structure, finish and shape of castings, and determine cutting rates reasonably accurate without prior testing. The common casting materials such as gray iron, carbon, alloy steel and malleable iron castings are termed free machining. Some of the heat resistant castings and titanium-base alloys are more difficult to cut and require a more thorough understanding of the saw blade and machine requirements to achieve maximum production sawing. In discussing cutting rates, it is necessary to consider three important factors, namely, finish, production and saw life. If prime interest is smoothness of finished

cut, one combination of factors is recommended—faster saw velocity, finer pitch saw and slower feeding rate. If the prime interest is fast production, then faster saw velocity, coarser pitch saw and faster feeding rate is recommended. If saw life is of prime interest, then a slower saw velocity, finer pitch saw and medium fast feeding rate is used. Optimum of all three cannot be had at the same time. Table 1 shows the path to follow to achieve these various results.

TABLE 1

| | For Better Finish | For More Production | For Better Saw Life |
|------------------------|-------------------------|---------------------------|---------------------------|
| Faster saw velocity | x | x | |
| Slower saw velocity | | | x |
| Finer Pitch Saw | x | | x |
| Coarser Pitch Saw | | X | |
| Slower Feeding Rate | X | | |
| Faster Feeding Rate | | x | |
| Med. Fast Feeding Rate | | - | x |

From the foregoing information, it is concluded that the technique to achieve fast production is different from that which is necessary to achieve finish and saw life. Consequently, in establishing the following cutting rates, these various factors were taken into consideration.

The foregoing cutting rates are defined to clarify thinking pertaining to various factors mentioned. When planning a production bandsawing program, we must consider: Material specifications, conditions



Photo shows friction sawing of gate from stainless steel casting.

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. TABLE 2-LOW SPEED SAWING RATES ON FERROUS CASTINGS
Cutting Rate sq in. min on 3-in. thickness using

| precision type blade | | | | | |
|---------------------------|--------------------|-----------------------------|----------------------|--|--|
| Casting Material | For Best Finish | For Best Production Rate | For Best Saw Life | | |
| Gray Iron | 0.5-2.0 | 2.0-6.0 | 1.0-3.0 | | |
| Carbon and Alloy Steel | 0.4-1.5 | 1.5-3.5 | 0.6-2.0 | | |
| Malleable Iron | 0.3-2.0 | 1.0-4.0 | 0.75-1.3 | | |

of finish, method of feeding, type of bandsawing machine and saw blade employed. The cutting rates shown were established when using carbon steel precision type saw blades. It is understood that by changing to line milling, line grinding or claw tooth type blades, the various results will change depending on the blade used.

Bandsawing Nonferrous Castings

Low and High Speed Sawing—Job foundries and mass production nonferrous foundries have a real problem of achieving and maintaining lower production costs in the cleaning room. This is true more so today than in previous years.

It used to be that a bandsaw was just a bandsaw, and that it required no particular knowledge to get the work done and come out on top from a standpoint of cost. To meet competition and keep production cost down, sawing must be understood more thoroughly: how material composition affects saw life, handling the work, surface finish, tolerance of cuts, production rate and tool cost.

Material Composition vs Saw Life-Composition and size of castings or size of gates, fins and stubs to be trimmed determine the type and speed of machine and proper saw blade to use. For example on aluminum, if the castings contain little or no silicon and are clean, the speed of the machine for cutting all thicknesses would be from 3,000 to 5,000 fpm. If the silicon content is greater than 1 per cent, the speed of the machine should be under 3,000 fpm. To illustrate: on No. A132 aluminum containing 12 per cent silicon and 2.5 per cent nickel, the saw velocity of the machine, to achieve best tool life, should be 100 fpm. Material composition only determines speed of machine, not the size nor weight of castings. It is always more economical for foundries casting several different compositions to employ variable speed machines which make possible rapid adjustment of saw velocity to the proper speed in accordance with material composition. In the case of medium-high-silicon castings, variable speed enables cutting at the proper predetermined velocity until such time that the saw blade becomes dull, then by simply speeding the machine up to around 8,000 to 10,000 fpm, continue cutting with a hogging action until the saw leads excessively or becomes too dull for efficient performance. This is sometimes called "bleeding the blade." It is a case of using the initial tool sharpness, then switching to another saw velocity or technique to achieve more production with the same blade. This approach is successful on cross-sections up to approximately 1 in. in

thickness; work thicknesses greater than 1 in. require only the lower velocity range. In the latter case, a good rule of thumb to follow is: the higher the silicon content or other wear elements present, the lower the saw velocity.

Handling the Work

Free flow of castings to the saw is of chief concern, a phase of production planning too often neglected. An overhead conveyor is efficient for medium to large size castings, while a belt type conveyor is best for small work. Equally important is getting the castings away from the saw after trimming. Since the object of efficient conveying is to create production flow and decrease handling time, more emphasis should be given to this problem. The band sawing operation should be strategically located so as to keep handling time of each casting to a minimum.

Surface Finish and Tolerance

The band saw is the only type machine capable of following the outline of the castings when removing gates and risers. Many foundries today are not utilizing band saws to the fullest extent by taking advantage of the latest technical advancement in band sawing machines and blading. It is of utmost importance to be able to clean a casting accurately, with one pass, to eliminate secondary cleaning operations and higher labor costs. To gain long uninterrupted production, a hard edge flexible back precision or buttress type blade should be used, the width and pitch of the blade depending on the work thickness to be cut. The general rule is the larger the gate, the coarser the saw pitch. For high speed sawing at least two teeth should be in the work at one time and for low speed sawing, three teeth. This rule will vary, depending on degree of smoothness of finished cut desired. In order to achieve the best finished cut relatively fine saw pitch, high saw velocity, and light feeding force should be employed (See Table 1). To secure a better finish, add lubricant in the form of a spray to the cutting edge of the saw. A spray type applicator is automatic, more efficient and saves the operator's time as compared with the intermittent hand application of a wax stick. In addition to lubrication helping to supply a better finish, it will also prolong saw life in some case as much as 40 to 60 per cent.

Production Rate

The higher the saw velocity the faster the cutting rate, but there is an optimum velocity for any casting material and over that speed, tool life diminishes rapidly. The cutting rates of silicon bronze, manganese bronze and aluminum-base permanent mold cast metals are comparatively low due to the lower velocity required to prolong tool life. Cutting rates vary considerably within a single group of metals as shown in the following table:

CUTTING RATE NONFERROUS CASTINGS

| Casting Materials | So | In. | cut/min | |
|-------------------|----|-----|---------|-----|
| Aluminum | * | 6 | to | 120 |
| Bronze | | 2 | to | 60 |
| Magnesium | | 10 | to | 160 |

Many foundry production engineers have made the mistake of recommending high velocity machines, when actually, low velocity was required. Machine velocity can only be determined by material composition and hardness of casting. Nonferrous casting materials with little or no silicon and free of iron require high speed machines, enabling fast cutting rates, at the same time produce long blade life.

Tool Cost

Since tool cost depends almost entirely on the sawing technique used and not the initial cost of the saw blade, it might be well to reiterate some of the influencing factors. A sawing engineer will take into consideration the machine's mechanical strength, balance, horsepower and saw velocity, operator's attitude towards optimum production, his strength, ability, and manual dexterity, material composition, hardness and surface condition of the castings. After the foregoing have been determined, then it is a question of which of the following is most important—smoothness of finished cut, production rate or tool cost. With all factors weighed, the initial cost of the saw blade is then added to determine ultimate tool cost.

Low Speed Sawing

Permanent mold cast materials, and other type wear resistant materials containing a high percentage of silicon, or other wear type elements, should be placed in the low-speed sawing group. Due to the abrading action of these materials, heavy feeding force must be exerted to prolong sharpness of the saw teeth. It is beneficial to apply lubricant, preferably with a spray applicator to assist in retaining saw tooth sharpness.

Hydraulic feed tables and quick action hold-down clamps prove quite practical especially for medium to heavy work. The work should be as free as possible from vibrations during the cutting cycle.

Regular contour sawing machines embody velocity ranges to fit the production within a particular foundry. Machines are now available with virtually any speed, making it possible to have any velocity range from 40 to 15,000 fpm. Until the recent introduction of the machines with speeds from 40 to 10,000 fpm the low speed range was from 50 to 1,500 fpm. The new speed range eliminates the necessity of employing two machines where there are both free-machining and silicon-type castings to be cut. By simply adjusting the speed to the proper saw velocity, according to the materials being cut, much greater saw life and cutting efficiency will result.

Uninterrupted Cutting

A controversial phase of bandsawing, when employing the chip forming technique, is the practicality of using one type saw blade as against another. Since the more abrasive resistant a cutting tool is, the longer the tool life. Therefore, there can be little question as to what type tools are desirable for long uninterrupted productions. High carbon, hard edge flexible back band saw blades meet most requirements. Carbidetipped band saw blades are now being manufactured and used on different tough materials and undoubtedly because of their ability to resist wear, they will become increasing popular for common foundry applications.

Conditions of Finished Cuts-Optimum bandsawing efficiency involves three major factors: (a) finish (b) tool cost and (c) cutting rate. The following rules of thumb apply to these requirements.

Finish—The faster the saw velocity, the finer the saw pitch, the slower the feeding rate, the better the finish.

Tool Cost—The slower the saw velocity, the finer the pitch, the greater the coolant and lubricant, the more constant the feeding force, the longer the saw life.

Cutting Rate—The faster the saw velocity, the coarser the saw pitch, the greater the feeding pressure, the faster the cutting rate.

Acknowledgment

The DoAll Company thanks the Cooper Alloy Foundry, Battelle Memorial Institute and P. R. Mallory and Company, Inc., for their grateful assistance in tests and materials.

ORGANIZING MANAGEMENT FOR PROFIT

By

Wally E. George*

MUCH HAS BEEN SAID, wherever men of management meet to discuss their related problems of business, about managing for cost. We would not deprecate a single constructive thought written or spoken on that theme. But management, as you all know, spreads far wider than the areas which cost control encompasses.

Management of product design, management of engineering and metallurgy, management of quality, management of finances, management of prices, and management of sales, all are of great importance to the success of a business. Between the making of a sale and the delivery of the product lie not only all the costs of the operation and all the functions of management, but an elusive, always sought after, margin called *profit*.

The American system of free enterprise is a profit system. Our nation leans heavily on revenues taken as taxes on corporate profits. We know that profits plowed back have built many of our great American industries. We are aware that if there were no profits, there would be no private funds available for industrial expansion. The entrepreneur in business would vanish completely.

The socialistic or communal governments decry profits as if they were attributes of the devil. In all probability, those who cry the loudest against them may be least aware that the derivation of the word profit is from the Latin word profectus, meaning advance or progress. Without profits our nation cannot progress. "Good times" are with us when industry and agriculture have profitable years and depressions come when they do not.

So it should be with the foundry industry. But is it? Statistics on foundry profits are not available expect in scattered instances. But we know that many American foundries have had rough going for nearly a year.

The steel foundries production as reported last fall was but 36 per cent of their 1943 level. Malleable was more fortunate. They experienced 52 per cent of 1943 output, the peak wartime year. Gray iron figures are

not reported in this fashion but scattered reports place the iron foundries rate of output close to 50 per cent of capacity and it appears that only half of them are making profit.

Well organized industrial concerns often find profits vanishing when their volume drops to such levels. Each of you may know how true this is within your own company. These considerations give rise to a serious and challenging question.

Are there too many foundries in the United Statesor in a number of concentrated industrial areas—to permit the industry to know "good times" in a year like 1949 when sixty million of our people were at work? Essential as the foundry industry is to the nation, it is inconceivable that it must be perpetually a marginal business.

We who spend our days in helping management do a better job of managing cannot be defeatists. We know there is profit in rightly managed business and when we find a company that is not profitable, we seek first a calm analysis of the cause.

Nine times out of ten our search leads us directly to top management or into certain sectors of management. Even in the tenth time of the ten, when we find a decadence in the industry itself, we still turn squarely to management to see why they overlooked such a fact, or why, if they were aware of it, they had failed to swing their talents and energies into product fields that were still expanding.

But when the industry is essential, when its products have a vital place in the needs of our industrial economy, when leading competitors of the client admit of steady profits, we turn our investigations and analyses to the commissions and omissions of the management. What is the breadth and depth and strength of the management? How sound is its thinking and planning and doing? What are its goals?

Modern business, it is true, is organized to be a profit making entity but rarely is profit its only goal. An industrial firm, consciously or not, is in business to make better products, to provide work and income and pleasure to its employees, to serve and help its public or its community, to contribute to the strength of our republic. All these are goals.

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Goals of a Business

A few years ago the following list of objectives was prepared for the president of an eminently successful company when his originally defined expression was for profits only:

1. Profits—Steady income that affords continuous dividends for the stockholders and reserves for the development of the business.

2. Management—A balanced organization of executives and supervisors that gives strength and coordination to the administration of the business.

3. Personnel—High grade, well paid people, loyal to their company and happy in their work.

4. Products—Superior products, known for their quality and integrity, that are developed in a continuous program of research.

5. Markets—Healthy and economically sound expansion of markets resulting from better products, services, and customer contacts.

6. Plants—Modern manufacturing plants, well located, and equipped with suitable machines and other facilities.

7. Costs—Low competitive costs that come with the application of scientific processes and efficient methods.

8. Prices—Adequate and fair prices that attract the customers and provide them with quality products.

9. Policies—Sound, progressive policies of business that build permanence and stability into the enterprise and insure its growth.

 Controls—Strong and measured guidance of the operations to assure financial strength and protection of the invested funds.

11. Pleasure—The organization of the business in a manner to provide the employees good working conditions and pleasure from their daily toil.

Administration of a Business

When one man sets out to accomplish these or other objectives of his business, he soon becomes aware of his inability to do the job alone. The best foundry executive in the world is still dependent on the strength of his organization. His is a job of inspiration, coordination, and leadership.

Where do the chief administrators of foundries come from? Very few rise to the top with a rounded experience in all phases of the business. Many come up from the molding floor, some from the financial end of the business, some from sales managements, and others from technical or engineering fields. No matter what his start, the wise executive is one who is conscious of his own limitations. He who has an awareness of his company's goals and builds up a staff of strong men around him will prove his right to leadership and to success.

The successful foundry presidents surround themselves with men that supplement their own talents. The chief executives must lead, and inspire, and push. They are planners of action and skilled in the interpretation of advice. They demonstrate their executive ability by producing results and by making the profits that assure stability and growth to their organizations.

Management Organization

We named strength of management as our second objective and rightly so. In our analysis of business problems, we find almost universally that our client's difficulties have arisen from organizational faults or from personnel weaknesses. Our recommended programs or solutions are frequently directed to improving both the organization structure and the executive personnel of the business. For when the structure is sound and the executives are competent, the methods of operation become a much simpler problem.

Organization has been defined as the plan or structure of the business in which the executive components are graphically portrayed to show relationships, points of required coordination, and lines of authority within the business. The organization charting must be backed up by descriptions of each position that will tell the authority, the responsibilities, and specific duties. An objective analysis and appraisal of the key men necessarily becomes an important part of an organization study if we are to be assured that the structure, as portrayed, is strong and workable.

Levels of Authority

The executive structure of a business must be planned with care. As we have suggested, when the operation of a foundry develops in size and complexity, it reaches a stage where no one or two individuals can assume all the authority for making decisions and taking necessary actions required of management. Hence, we have to have levels of authority and even a foundry of 200 to 400 employees may have these seven levels:

 The stockholders or owners who have invested their capital and expect reasonable and steady returns.

2. The board of directors, with a chairman, who determine the goals, approve the policies, and guide the management.

3. The *president*, who works with the board on setting objectives and policies and who administers the organization.

4. The operating committee, on the vice presidential level, who pool their executive judgment and experience, to assist the president in his major decisions and plans.

5. The *key executives*, officers and managers in charge of major divisions and departments, who administer the activities of their areas to achieve the goals and to carry out the approved policies.

6. The department heads, or superintendents, who are responsible for supervising activities assigned to them in such a manner as to assure adherence to goals, policies, and schedules.

7. The *foremen*, or section heads, who are personally responsible for smaller areas of a major department and are the direct points of contact with the balance of the employees.

Basic Steps in Setting Up a Sound Organization

When a foundry wants to reconstruct its organization or to formalize it for the greater strength and coordination that should ensue, it should move along the lines of a carefully thought-out plan. There are five steps it can take to provide greater assurance of success:

- 1. Establish and define the major divisions, departments, and sections required to embrace all operating elements of the business.
- 2. Agree upon an operating plan of organization and spell out the functions of each unit.
- 3. Clarify the understanding of relationships among the major divisions and within all their subdivisions.
- Determine the executive or supervisory positions and decide on the personnel requirements.
- 5. Provide a clear delineation of authority and responsibilities and duties assigned to each individual.

Principles of Good Management

An organization's success in rebuilding its management structure depends, so experience says, upon its adherence to certain sound principles of good management. What are these principles and how can an operating executive make best use of them?

We offer a concise list of ten basic principles in Fig. 1. They are not so difficult to use when one decides to adopt and follow them. Their adherence is imperative, however, if one informs his management team of their adoption. To illustrate their usage, and the five basic steps which we have listed for setting up an organization, we shall take a case study of a gray iron foundry of 295 employees for which we developed a revamped organization.

PRINCIPLES OF GOOD MANAGEMENT

- The responsibilities of each executive and employee should be definite and clear cut.
- 2. Any executive should assume absolute responsibility for the acts of all who report to him.
- 3. There should exist a clear line of formal authority from top to bottom in every organization.
- The work of every executive and every employee should be confined as far as possible to the performance of a single leading function.
- Every executive and every employee should receive orders from, and be responsible to, only one authority.
- All problems involving dispute of authority or responsibility should be settled objectively and promptly.
- Changes in responsibility and authority should be so made that all persons concerned will have full understanding of them.
- Orders should never be given to employees over the head of the responsible executive to whom they report.
- The practice of executives or employees going over the heads of their immediate superiors should be thoroughly discouraged.
- 10. Criticism at all times should be constructive.

Fig. 1-Principles of Good Management

Planning the Organization

This company had two lines of business, (1) specialty castings produced in quantity and to trade specifications for a wide spread market and (2) jobbing work to accompany the specialty orders and miscellaneous castings for the local buyers.

This foundry had a corporate worth of nearly a

million dollars. It had a board of directors that were mostly non-foundry business men or representatives of the scattered, original owner interests. It had a board chairman who was a banker and a president who had been in this particular field of foundry work for 35 years.

Its operations, from our point of view, fell naturally into three major divisions, mainly sales, production, and finance. But within the company, the operations were confused and overlapping. Sizeable plant expenditures and the advent of a new operating head had accompanied a healthy resurgence of their specialty business after World War II. The president, through a death and a retirement, had unconsciously become the financial man. Lines of authority for engineering, purchasing, production control, and cost control were not clearly drawn. The executives, in general, were too few in number and were spread too thin.

In short, this foundry may have been no different from your own or from any one of several that you know. Our task was that of building a workable and practical organization structure for our client so that he could realize the full profit potential of his business and could work out the last ten business years of his life in company with a purposeful, happy, and prosperous group of executives and supervisors around him.

We started out to work with the president and his two key men in sales and production on the determination of the top organization structure. We reached agreement on the position of the board of directors and on the authority of the president. We worked out the creation of an operating committee to afford the president orderly advice and assistance from his top executive team. We thus replaced one-man judgment with the balanced and guided thinking of the real operating heads of the company.

STOCKHOLDERS

The separation of the overall operation into the three major divisions of sales, production, and finance was easy. For better division of the working load, however, the operations of purchasing and personnel were also placed directly under the president. Agreement on the proper placement of the other operating functions within the three divisions was more difficult. But finally we reached general acceptance on the top management chart, which shows as Fig. 2.

Here you will see the designated title in each instance, the line of authority, and the stated responsibilities of each position. Important to the success of a new group of operating charts is the understanding of each key man of the authority and responsibilities of his boss, himself, and of others on the same chart.

Also necessary to the whole understanding, for each of these key men, is how they plan to delegate their authority and responsibilities to their own lesser teams. In order to follow this principle out all the way, a series of two-level charts is required to carry on down this top one to the lowest level of supervising and technical management.

Figures 3, 4 and 5 display respectively the charts of the Vice President of Manufacturing, the Vice President of Sales, and the new Secretary-Treasurer. For the latter two divisions there was no need for further charting at lower levels. For the Manufacturing divi-

BOARD OF DIRECTORS

Approval of broad company-wide objectives, policies, and plans. Election of officers.

Election of different of dividends.

Authorization of depositories for funds.

Approval of company-wide budgets and major expenditures.

Approval of company-wide results against established objectives.

PRESIDENT

Responsible for:

Organization and administration of the company. Organization and administration of the company wide objectives, policies, and plans. Formulation of company wide financial and operating budgets. Primary selection of officers.

Approval of key personnel.

Coordination of sales, manutacturing, finance, and control. Maintenance of sound public, industry, and employee relations. Evaluation of company-wide results against established objectives. Approval of salaries and of changes in wage and salary schedules. Serving as a member of the Operating Committee.

Fig. 2-Plan of Organization for Top Management

OPERATING COMMITTEE

Responsible for:
Conseling weekly with president on company-wide policies and plans.
Review of proposed capital expenditures.
Review of statries and proposed ment increases.
Review of proposed major changes in selling prices.
Review of proposed changes in the hourly wage schedule.
Review of long-range manufacturing scheduling.
Review of long-range manufacturing scheduling.

MANUFACTURING DIVISION Vice President

SALES DIVISION Vice President Responsible for:

Organization and administration of the manufacturing division. Coordination of the manufacturing division with the other major divi-sions of the company.

Organization and administration of the sales division. Coordination of the sales division with

Responsible for:

the other major divisions of the company. Formulation of sales objectives, poli-

Responsible for: Primary selection of plant personnel. Maintaining personnel records and Study of regional and company wage Coordination of the merit rating pro Assistance in labor contract negotia-Administration of the plant accident prevention, bealth, and fire hazard Providing first air and medical care

Personnel Director

PERSONNEL

Formulation of manufacturing and

Direction of all manufacturing to meet established quality, quantity, and cost standards. personnel objectives, policies,

> Direction of sales of the company's Maintenance of sound industry and

products.

cies, and plans.

Direction of product research.

Maintenance of an adequate labor Creative development on special ma-

Provision of manufacturing facilities. Selection of key personnel. Evaluation of manufacturing results against established objectives. Serving as a member of the Operating Committee. chine tools.

> Selection of key personnel. Evaluation of sales results against Serving as a member of the Oper-ating Committee.

established objectives.

Administration of employee services such as group insurance, hospitali-zation, and bond purchase plans.

to injured employees.

programs.

Investigation of customer complaints.

Sales engineering and customer quo

Analysis of markets. customer relations.

Handling of grievances.

structures.

Advertising.

FINANCE AND CONTROL DIVISION Secretary-Treasurer

Responsible for:

Organization and administration of the finance and control division.

Coordination of the finance and control division with the other major divisions of the company.

Formulation of finance and control objectives, policies, and pland direction of financial and accounting controls and records.

Assistance to top management in the development of organization plans, policy manuals, and administrative methods and procedure.

Receipt, disburement, and custody of funds.

Retaindains with banks.

Credits to and collections from customers.

Protection of company assets by adequate insurance

Perparation of corporate reports.

Preparation of corporate reports.

Legal matters.

Management of the office services and facilities.

Preparation of the salary payrolls.

Selection of key personnel.

Evaluation of financial and control results against established objectives.

Serving as a member of the Operating Committee.

Purchasing Agent PURCHASING

Responsible for:
Purchase of all raw materials, equipment, and aupplies.
Scheduling and follow-up of deliveries
Of purchased items.
Development of sources of supply.

Maintaining files of vendor's catalogs, bulletins, and prices. Approval of vendors' invoices. Arrangements with vendors for adjustments on substandard merchan-

Keeping top management currently informed on the procurement, shipment, and inventory of major raw

STOCKHOLDERS

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PRESIDENT

Fig. 3-Plan of Organization for Manufacturing

Division

MANUFACTURING DIVISION

Vice President

Responsible for:
Organization and administration of the manufacturing division.
Cool anaton of the manufacturing division with the other major divisions of the company.
Formulation of manufacturing and personnel objectives, policies, and plans.
Direction of all manufacturing to meet established quality, quantity, and cost standards.

and cost standards.

Maintenance of an ad-qua e labor force.

C.vat.ve development on special machine tools.

Selection of manufacturing facilities.

Evaluation of manufacturing results against established objectives.

Evaluation of manufacturing results against established objectives.

PRODUCTION CONTROL Production Coordinator

> PRODUCTION Superintendent

ENGINEERING Chief Engineer

METALLURGY Metallurgist

Responsible for:
Production of the company's products
in accordance with established quantity, qual by, and cost standards.
Disection and coordination of all production departments and personnel.
Counset and assistance to the foremen
in their day-to-day operating prod-

ings.
Providing engineering services and direction in the construction of plant buildings, machinery, and

Responsible for:

Development of quality control standards for raw materials, processes,
and molten iron.

Chemical and physical analyses of
molten iron, raw materials, and
finished products to check conformance with established quality standards.

Responsible for:
Design and specifications of buildings, machinery, and equipment.
Development of detailed layout draw-

Responsible for; Conduction plans with also objectives.

Development of long-range plans for the economical and orderly manufied economical and orderly manufied the company's products. Development of day-lo-day production achedules for the foremun schedules for the foremun schodules purplies in accordance with production plans and schedules. Receiving all materials and the company the company the company controlling raw materials

Maintenance of plant facilities.
Selection of foremen.
The practicipation in the determination of salaries and wages for operating d partment employees.

Merit rating of foremen's perform-

equipment.

Cornulating product specifications in accordance with established policies.

Keeping informed of engineering developments in the industry.

Direction of the industrial engineer-

Counsel to operating department as to disposition and use of raw material. Preparation of physical and chemical

test reports.

mixes. Research pertaining to new raw materials and products. Keeping informed of metallurgical developments in the industry. Development of formulas for required

ing function. Supervision of the engineering de-

Chief Inspector INSPECTION

and supplies. Shipment of finished products. Handling of traffic matters.

Responsible for: Inspection of all products manufactured by the company to assure conformance with established specifications and quality atandards. Rejection of items which do not conform with established specifications in terms which do not conform with established specifications

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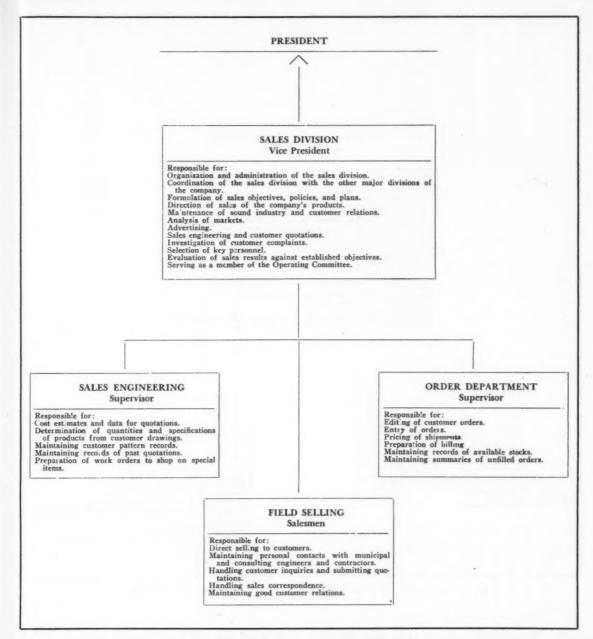


Fig. 4-Plan of Organization for Sales Division

sion there were a series of other charts. Those charts for engineering and production control each concluded their lines of authority, while that for the production departments under the superintendent, Fig. 6, called for still another level of charting to define responsibilities one step lower. Thus, under the superintendent are separate charts for the general foreman of the specialty foundry, the general foreman of the jobbing foundry, and the master mechanic. We show the one for the jobbing foundry in Fig. 7 to illustrate this level of charting.

A glance back at this series of six charts will show that we have displayed all the level of management from (1) owner, to (2) board, to (3) president, to (4) vice president, to (5) superintendent, to (6) general foreman, and (7) finally down to the foreman.

We are not showing the engineering chart for developing the functions of the industrial engineer. These are staff positions outside of the line of authority emphasized in this seven-level relationship and we have dealt primarily on the producing line or end of the foundry business.

Industrial engineering normally includes methods engineering and standards, or timestudy, engineering. It may also embrace cost control. The ultimate success of a production operation quite often depends on the engineering that is behind it in planning the facilities, the processes, and the layout for the individual work

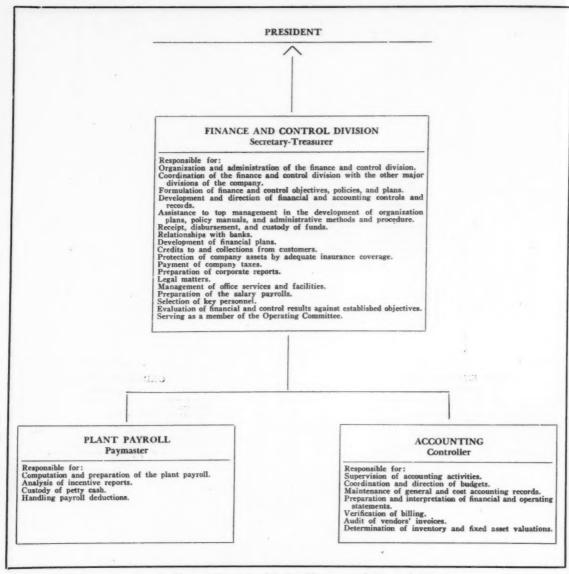


Fig. 5-Plan of Organization for Finance and Control Division

stations. The expected profit in every sales price is best realized when we have sound standards for every operation, accompanied by wage incentives and a system of accurate control reports to assure the proper cost.

To you who are unfamiliar with management thus modernized, we wish to make clear that we have not burdened this client organization with too many charts. Rather, we have defined and clarified its thinking, and have rendered understandable its delegation of authority and control for every member of its entire management team. Its success will be in keeping with the caliber of the 40 men assigned to the executive and supervisory positions.

Best of all, in a formalized management structure, the top executives will not be bogged down with routine or detail. They will have time for constructive thinking and will have internal assistance to guide their judgment. Decisions will be prompter, direction will be more purposeful, controls will be enhanced and supervision strengthened, and greater profits will emerge to reward the efforts of all concerned.

DISCUSSION

Chairman: R. L. LEE, Grede Foudries, Inc., Milwaukee. Co-Chairman: V. E. ZANG, Unitcast Corp., Toledo.

J. A. Forbes: 1 Do you think it wise for the inspection department, or the chief inspector, to report to the foundry superintendent rather than to the general manager?

MR. GEORGE: In one of the charts, we show the chief inspector reporting directly to the vice-president in charge of production. He was not under the foundry superintendent. I would rather not see the man who makes the castings direct the chief inspector because I think it brings out too many arguments and disputes over the responsibility of quality. We would prefer to see the superintendent and the chief inspector independent of each

OPERATING VICE PRESIDENT

Fig. 6-Plan of Organization for Production Depart-

PRODUCTION Superintendent

Responsible for:
Production of the company's products in accordance with established quantity, quality, and cost standards.
Direction and coordination of all production departments and personnel.
Counsel and assistance to the foremen in their day-to-day operating problems. Maintenance of plant facilities.
Selection of foremen.
Participation in the determination of salaries and wages for operating department employees.
Merit rating of foremen's performance.

YARD GANG Foreman

MAINTENANCE Master Mechanic

Responsible for:
Unloading cars on tipple and other
materials as directed.
(leanup and d'sposal of refuse.
Maintenance of yard and grounds.
Supervision of the yard gang.

Responsible for:
Coordination and direction of electrical, mechanical, and building maintenance departments and watchmen.
Planning and scheduling construction
Work.
Development of a continuing preventive maintenance program.
Coordination of maintenance functions with production requirements.
Maintaining an adequate maintenance.

Assistance to the foremen in the final selection of employees temporaries of collaboration with the foremen in the merit rating of employees. Supervising details of new construction.
Assisting in estimating costs of finished products or projects.

JOBBING FOUNDRY General Foreman

Responsible for:
Coordination and direction of the pattern shop, foundry and eleaning departments, and the movement and storage of special foundry Mondates.

Maintaining production accordance with production achtering with production achtering my expension according with production achtering with grandards and at economical costs.

iron.

Handling of raw materials into the cupola.

Maintaining production schedules, quality standards, and economical Responsible for: Supervising production of molten

Impection of cupola and ladle equipment and assuring that they are in proper condition for use. Sanitation, safety, and good housekeeping. Requisitioning materials needed for

Meaning an adequate working force.

Assuring that production equipment is in good working condition.

Assistance in the estimating of labor coasts on special items.

Assistance to the foremen in the final selection of employee.

Collaboration with the foremen in the mail and ment rains of employee.

SPECIALTY FOUNDRY General Foreman

CUPOLA Foreman Responsible for:
Coordination and direction of the Coordination and direction of the specialty foundry, the cleaning Troom, and the heat treating.
Maintaining production in accordance with production in accordance with production achedules, quality specifications, and cost standards. Training and development of speciality machine operators to assure an adequate working force.

Direction of the application of incentive paradards within the depart. Assistance on the planning of special Production.

Recording materials used and molten iron produced.

Accurate time accounting required for cost system.

Final selection of employees.

Appraisal of employee performance.

equipment and tooling.

Collaboration with the foremen in the merit rating of employees.

on. ner tor tes the

SUPERINTENDENT

JOBBING FOUNDRY General Foreman

Fig. 7-Plan of Organization for Jobbing Foundry

Coordination and direction of the pattern shop, foundry and cleaning departments, and the movement and storage of special foundry Responsible for:

products.

Maintaining production in accordance with production schedules, in kerping with quality standards and at economical costs.

Maintaining an adequate working force.

Assistance in the estimating of labor costs on special items.

Assistance in the estimating of labor costs on special items.

Assistance to the foremen in the final selection of employees.

Collaboration with the foremen in the merit rating of employees.

NIGHT FORCE Foreman

CLEANING DEPARTMENT

Foreman

Responsible for: Performing work assignments at night as dele-gated by the general foreman.

MOLDING DEPARTMENT Foreman

> PATTERN SHOP Foreman

Responsible for:
Supervising the machine and floor mold casting
of fittings.
Maintaining easting production schedules, quality
standards, and economical costs.
Inspection of patterns, molds, and foundry
equipment and assuring that they are in good
operating condition. The production for defects apparent before cleaning.
Inspection and control of sand and other ma-

Responsible for:
Supervising production of foundry patterns.
Ma.nta.ning pattern production schedules, quality standards, and economical costs.
In spection of pattern shop equipment and assuring that it is in proper condition for use.
Assistance to the patternmakers on complicated

patterns.
Maintaining the pattern shop storage room.
Checking patterns in and out of the storage Sanitation, safety, and good housekeeping. Requisitioning materials needed for pattern pro-

terials. Sanitation, safety, and good housekeep'ne.
Requisitioning materials needed for production.
Recording materials used and items produced.
Accurate time accounting required for cost

system.
Final selection of employees.
Appraisal of employee performance.

Accurate time accounting required for cost Recording materials used and patterns produced.

system.
Final selection of employees.
Appraisal of employee performance.

Responsible for:
Supervising the cleaning and coating of special foundry castings.
Maintaining Production schedules, quality standards, and economical costs.
Inspection of cleaning shed equipment and assurlaspect on and weighing of castings.
Delevery of eastings a storage yard, machine shop, or other designated points.
Delevery of castings to storae yard, machine shop, or other designated points.
Delevery of castings to storae yard, machine shop, or other designated points. chine short.

Sanitation, safety, and good housekeeping.

Requisit onner materials needed for production.

Recording materials used and items produced.

Accurate time accounting required for cost

system.
Final selection of employees.
Appraisal of employee performance.

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other.

CHAIRMAN LEE: A question was brought up regarding the foreman doing the cost estimating.

MR. GEORGE: If the man responsible for production does the estimating, he will be tempted to be somewhat conservative so that he can meet his estimate without having to push too much to get production out at the estimated cost. Yet, in some small foundries the foundry foreman is the only man who can do the estimating.

CHAIRMAN LEE: According to your charts who does the estimating?

MR. GEORGE: It is the responsibility of the vice-president in charge of manufacturing or a personal representative. The chief engineer and the superintendent in a case cited got together and planned every job. They planned for low cost and for a profit on the right selling price. While the superintendent might have held out for an easier cost to make it readily possible of achievement, I think he was well aware that they wanted to get the business and that they had to produce at the lowest cost. He knew little was to be gained by setting an easier mark for himself. Cost control ties in the estimate with the reality and it is often difficult to set a tough, close estimate and then meet it.

I gave talks to foundrymen on meeting costs. I pointed out that in the sales dollar there was an expected amount of profit over and above the cost. Now, if you have been precise in that estimate, you expected to meet a definite cost figure. Anything that tends to increase that cost is going to whittle down your profit.

Inclusion of any operation that was not specified, use of slow machines, use of higher priced materials, all those tend to whittle down that expected profit. The first thing you know, what you expected to be 100 per cent or so, gets down to 3, 4, or 5 per cent. Maybe you just swap dollars. In that case that profit vanished entirely and the cost offset the entire sales dollar.

I know of one case where a chief metallurgist of a customer came in and said that the chromium was too low in a particular lot of castings. The chief metallurgist raised the chrome content without consideration to the cost estimate.

MEMBER: In the small foundry where the foreman does the estimating, if and where you cannot have full time job evaluations or studies and you call on consultants to set up these job evaluations, could the foreman simply follow the consultant's plan in estimating new job? It would have been catalogued for many so that there would be no temptation to set it lower or higher as the case may be.

Approximately how many operations would he have to check in running estimates in a foundry where he had several of those to do in a day, for instance, on a squeezer operation?

C. T. HASSELL: In using element standard times to set production standards, I think a good estimate would be about 3½ to 5 min to set a rate. I do not know how many rates they would need to set in your foundry unless I knew the number of new orders they were running each day. I think that they can set a squeezer rate in 3½ to 5 min and on small cores, bench cores, possibly about the same. It involves examining the

In regard to the application of element standard times, I believe that if it is properly set up and if you have the foreman's cooperation, you will get satisfactory results. However, if the foreman specifies elements of work that are not required to make the job, you will get inflated standards. In such cases the foreman is not qualified to set the production standards.

CHAIRMAN LEE: In 1920 we had 14 molders and about 6 coremakers in our shop. If we used sound judgment we would not have the melding foreman set molding prices or the core room foreman the core prices. We might have reversed it because we found over a period of years, that the molder belittles the core room foreman and the core room foreman does not think that the molding foreman knows anything, and there is a tendency to increase prices. In a shop of that size it would be an economic advantage to retain some impartial person to set those prices even if he did not know as much about it as the foreman did.

MR. GEORGE: Mr. Hassell will agree that no one should get a consultant and install a timestudy system without having an industrial engineer or department of their own. The foundry-

man must maintain whatever is put in by any consultant. You just cannot buy something out of a package or out of a box and think it is going to work for the next six months or six years. It will not work at all. You have to have that timestudy department of your own.

You will find that a department of that kind, where there is one, three, or ten men, will be self-sustaining. It will more than pay for itself. The industrial engineer will become the natural estimator. After he has accumulated this timestudy data, he can predict in advance that the production rate is going to be 12 cents or 12 pieces an hour. The timestudy man will be right in the center of this estimating at all times.

A. J. FRUCHTL: ⁸ In a larger organization, sometimes they have within the organization a cost standard department. Should this department report to the industrial engineer or to the accounting division?

MR. GEORGE: The industrial engineer in a large company should have three functions. He should be responsible for (1) a methods engineering section, (2) the incentives, and (3) cost control as distinguished from the work of the controller or the accountant.

Somebody has to be the go-between, between the front office and the men out in the shop who are signing for the cost of material and labor. In that sense we can believe that the industrial engineer can perform a good cost service on cost control work for the foundry superintendent.

As to cost standards, they become a part of a standard cost system. The controller or the chief accountant runs the standard cost system in a straight line of a large manufacturing company

Many a foundry runs on a job cost system or an estimated cost in advance, and the estimated cost becomes the cost standard. The record of keeping actual costs against that cost standard is an accounting function.

The job of doing something about it to prevent the cost from being too high is, to us, an engineering or a staff function. That is where the industrial engineer becomes the go-between of the front office and the factory.

It is too late to wait until the 15th of next month to find that in the first week of this month your cost was too high. You just cannot wait that long. You have to know the daily and weekly costs as you go along. That becomes an engineering function because when the front office, the controller, or the cost accountant just send out a piece of paper, nine times out of ten, nothing is done about it. However if it is brought out by an engineer, if it is explained, and if this engineer can help the foreman analyze where it was too high, why it was too high, or what has happened then one does not wait until the middle of the next month to take corrective action.

CHAIRMAN LEE: I am a bit puzzled as to what you mean by "front office." My thought was that this department should be answerable to what I would think of, not as front office, but either a management committee or the president. Is that what you meant by "front office"?

Mr. George: That is where you do your accounting; your treasurer and controller make their headquarters there.

The industrial engineer is primarily a shop man. We have them in most cases working for the works manager in a single-plant company. He is right out there almost next door to the superintendent's office. But he is one that goes up front to get the data, to analyze the figures, and who brings them out and translates them to the shop man and the foreman in the department.

CHAIRMAN LEE: You are referring to the superintendent, the works manager, etc. and this makes it confusing. I would suggest you foundrymen draw up your own organization chart and show what the responsibilities of different people are. You will understand what I am trying to emphasize is what is a works manager, what is a plant manager, what is a superintendent, etc.

You cannot talk loosely because the minute you do that you get away from an organization chart and you should have a carefully prepared organization chart. You should adhere to it after your industrial engineer has put it in for you. You cannot use a stereotyped organization chart because no two of you would have identical situations or the need for the same functions in your business. The chart that works well for one company will not work well for another.

MR. GEORGE: You have to plan your own chart around the people you have and the functions of your business and the

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² Manager, Industrial Engineering, Lester B. Knight & Associates, Inc., Chicago.

⁸ Resident Manager, U. S. Pipe & Foundry Co., Birmingham, Ala.

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things that you have to accomplish.

MEMBER: Mr. George did a fine job in developing our own organization chart. We found, in an organization which has been functioning for many decades, considerable confusion as to who reports to whom and just what he does, so naming names and titles is only part of it. It is who gets the work done and whom you put the finger on if he does not get it done.

I would appreciate more discussion on foundries working at low capacity. Is it the staff's place to know where we stand? You cannot do anything else, can you?

MR. George: A foundry that experienced a war peak of 300 tons a month may be running only 10 tons a day now. That makes it a different kind of business. You cannot maintain that organization structure that you set up for a peak capacity. If you are going to operate today for profit, you will have to work down that fixed dollar load. You can have only enough good people to run the size of business as it now stands. You have to

stop and look where you are going. You should ask: "How is it

possible at all to make a profit?"

I know of a company in Milwaukee that has gone through several sales and changes in management since the first World War. I know it was sold at one time for \$55,000 and it might have cost \$500,000. The man bought it, put in a new organization and began to run it on a different level entirely than what it had been in the 20's or in World War I.

Many foundrymen today will have to give up their thoughts of getting back to 1943 and 1944 tonnage levels. They will have to look carefully at what they can make with their facilities, and at what they can sell to their customers who buy the castings, and they will have to make a profit on that level. It is a big problem but it is not an unsurmountable one.

CHAIRMAN LEE: Even if you do not have any change in your volume of business, if your organization charts have been in existence for from 6 to 12 months, and most of them have, you have had changes in it.

MR. GEORGE: Every business changes constantly.

DILATOMETER STUDIES OF NODULAR CAST IRON

By

N. A. Ziegler,* W. L. Meinhart,** and J. R. Goldsmith***

Introduction

NODULAR CAST IRON has been attracting the attention of many metallurgists and foundrymen over a period of the last two years. The amount of work done and information accumulated on this interesting engineering material can be judged from the bibliography appended to this article.

The present authors have been experimenting with nodular iron for a considerable period of time. By melting cast iron of a suitable composition in a 200-lb high frequency induction furnace and adding to it between 0.15 and 0.50 per cent magnesium (either in the furnace or in the ladle) a nodulizing effect can be produced in castings of any wall thickness up to 2 to 3 in. The addition of about 0.25 per cent magnesium has been found to be a good general figure. After the magnesium treatment, the metal should be inoculated.

Good quality iron should be used as a base metal to achieve satisfactory results. The best properties were obtained with a final product containing about 3.0 to 3.5 per cent carbon, 2.0 to 3.0 per cent silicon, 0.5 to 1.0 per cent manganese, 0.03 per cent max sulphur and 0.15 per cent max phosphorus.

Carbon contents under 3 per cent will result in a stronger metal, but it will be more difficult to machine. Less than 2 per cent silicon will produce white iron, while over 3 per cent silicon will cause ferrite to be too brittle. Manganese, which is a carbide stabilizer, should be kept low. However, for a reason which is not yet well understood, lower percentages than 0.3 per cent manganese resulted in poorer physical properties than when it was kept between 0.5 and 1.0 per cent.

Magnesium is a powerful desulphurizer and, when added to molten iron, reacts with the sulphur first. In order to produce nodulizing effects, it thus becomes necessary to either use a low sulphur base meta' (about 0.03 per cent S max) or to desulphurize it by some conventional method, such as soda ash, or

to add to it excessively large amounts of magnesium, which might be expensive and impractical.

Phosphorus is not affected by magnesium. It forms a brittle iron-carbon-phosphorus eutectic, and, when present in amounts over about 0.15 per cent reduces the properties of the metal.

Magnesium might be added either as commercially pure metal, or in the form of a master alloy with some other metal, such as nickel, copper or silicon. The best results were obtained by using 80 per cent nickel-20 per cent magnesium alloy. Higher magnesium alloys, as well as "pure" magnesium result in a violent reaction and low magnesium recovery. Copper magnesium alloys containing about 30 per cent magnesium or less react with the molten iron rather slowly and form heavy oxide or slag which is difficult to remove.

Magnesium-bearing ferrosilicon (nominal composition—45 per cent Si, 2 per cent Al, 7-8 per cent Mg, bal. Fe) gives inconsistent results. Apparently its silicon/magnesium ratio is too high. When it is added in amounts necessary to introduce the desired amount of magnesium the iron becomes overdosed with silicon and graphite has a tendency to be of the flaky type. If magnesium-bearing ferrosilicon could be made to give reproducible and reliable results, its advantages would be lower cost, freedom from introducing "alloying elements" and combining nodulizing and inoculating treatments into a single step.

By using a proper base metal of the aforespecified general composition, treating it with about 0.25 per cent magnesium added as 80 Ni-20 Mg alloy, and inoculating it with about 0.5 per cent silicon, the present authors consistently can produce the following physical properties in the resultant nodular iron:

Tensile Strength - 70-90,000 psi Yield point - 50-70,000 psi Elongation - 10-15% Hardness -150-200 Brinell

One thing peculiar to nodular iron, which, perhaps, has not yet been sufficiently stressed in the published literature, is that in cross-sections of about ½ in. and up to perhaps 3 in. properties and structure are not affected by the wall thickness. In cross-sections de-

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 Chicago.

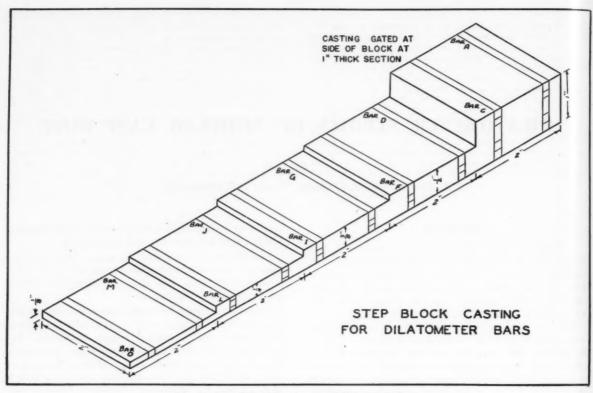


Fig. 1-Step Block Casting for Dilatometer Bars.

creasing below ½ in. the metal rapidly becomes white and hard. The amount and size of graphite noduli decreases in proportion.

Hence, the above physical properties, as measured by a 0.505-in. diam 2-in. gage bar, machined from a steel-type coupon block, sometimes can be obtained even in as-cast condition. Results obtained from cast bars (gray or malleable type) are unreliable, probably because of surface imperfections.

This, however, does not mean that small and thinwalled castings made from the same metal will have similar properties. Quite to the contrary, they are sure to be hard and will contain considerable amounts of primary cementite. To make their properties comparable to those of heavy castings, the thin walled ones must be heat treated. For heavy castings heat treatment is less critical and sometimes might be omitted.

This raises a new question in those instances when a casting is composed of heavy and light cross-sections. To get a better idea as to what happens in such cases, the present authors undertook a series of experiments now to be described.

Experimental Procedure

It was decided to subject nodular iron to thermal studies, using the self-recording differential dilatometer. This instrument had previously been used in our thermal studies of gray⁶² and malleable⁶³ iron and produced some interesting information.

For the present set of experiments, a step-type block, shown in Fig. 1, was selected as the experimental casting. It might be noted that it is 2 in. wide and con-

sists of five square steps, 1, $\frac{1}{2}$, $\frac{3}{8}$, $\frac{1}{4}$ and $\frac{1}{8}$ in. thick. Two dilatometer samples, about $\frac{1}{2}$ in. long and $\frac{1}{8}$ in. diam were machined from each step.

The present experimentation involved two such blocks, made from two different heats, thus resulting in four sets of dilatometer samples, i.e. four samples per step-thickness. Table I gives the chemical analyses of the two blocks.

| TABLE 1 | | | | | | | | j | | | | |
|---------|-----|------|------|------|-------|------|------|------|------|----|----|----|
| | | | Si | Mn | S | P | TC | Ni | Mg | Mo | Cr | Cu |
| Heat | No. | 6095 | 2.60 | 0.73 | 0.026 | 0.13 | 3.74 | 1.06 | 0.18 | Tr | Tr | Tr |
| Heat | No. | 6096 | 2.11 | 0.82 | 0.028 | 0.12 | 3.70 | 0.91 | 0.18 | Tr | Tr | Tr |

With the exception of silicon, the analyses are reasonably uniform. The lower silicon casting (No. 6096) had in thinner sections somewhat larger amounts of primary cementite. Even though lower silicon might slow down graphitization during the heat treating cycles, it should not affect too much the basic observations of these experiments.

Each section of each block has also been subjected to microexamination in as cast condition. It was found that, as it should be expected, there was a great deal of difference between sections of different thicknesses within each block.

Figure 2 represents as-cast structures of 1 in. and $\frac{1}{2}$ in. sections. It displays a predominently pearlitic background. Most of the graphitic noduli are surrounded by ferritic rims, giving the so called "bull's eye" effect. In the $\frac{3}{8}$ in. section, the width of these rims, as shown in Fig. 3, decreases, thus decreasing the amount of free ferrite. When the section thick-

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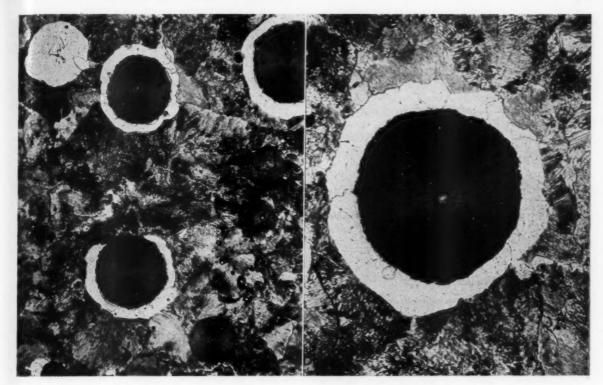


Fig. 2-Microstructure of 1-in. section, as-cast. Nital etch; x 400.

Fig. 3-Microstructure of 3/g-in. section, as-cast. Nital etch; x 1000.

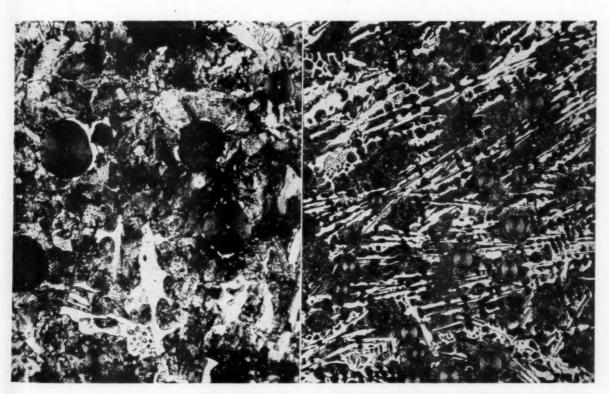
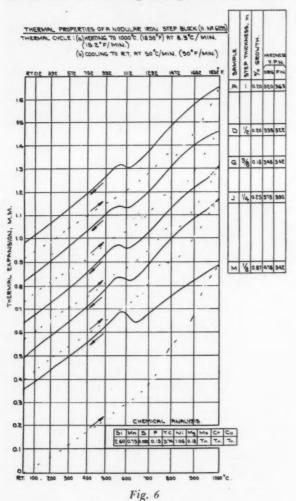


Fig. 4-Microstructure of 1/4-in. section, as-cast. Nital etch; x 400.

Fig. 5-Microstructure of ½-in. section, as-cast. Nital etch; x 200.

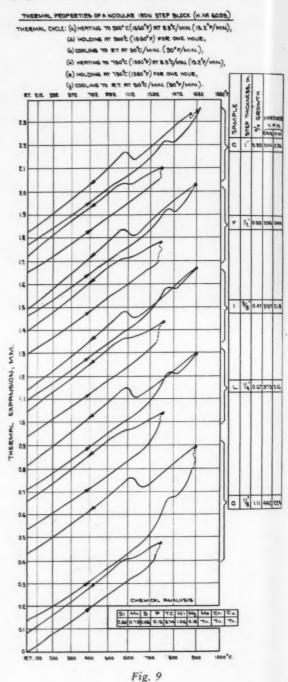
ness is reduced to $\frac{1}{4}$ in. free ferrite as shown in Fig. 4, disappears, and in its stead some primary cementite can be observed. Finally, when section thickness is reduced still further to $\frac{1}{8}$ in., the amount of primary cementite, as shown in Fig. 5, becomes quite appreciable, similar to that of white iron.

Sets of five samples (one from each section of the step block) were then subjected to three different dilatometer cycles. The resultant curves have been compiled in Fig. 6, 9 and 11 by tracing them separately for each cycle from the original photographic recordings. Dilatometer light, recording original curves, was put on 1-min time exposure at 5-min intervals during certain periods of the cycles. Hence, parts of the thermal curves in Fig. 6, 9 and 11 are represented by a succession of dots, rather than by solid lines. This gives some idea of the rates of transformations occurring during dilatometer cycles.



In Fig. 6, 9 and 11 each curve is designated by a letter, referring to the position of the corresponding sample with respect to the original casting (Fig. 1). Each curve is also marked with the percentage growth, which took place during the dilatometer cycle, as well as with the original and final Vickers (similar to Brinell) hardness of the sample.

During the dilatometer cycle, the sample is kept in a vacuum of about 5 microns. Hence, permanent dimensional changes (linear growth) are due to graphitization and not to oxidation.



Each dilatometer cycle was selected with the primary object of studying graphitization of either primary cementite at temperatures above the transformation range or pearlitic carbides within the transformation range.

In Fig. 6, 9 and 11 temperatures are indicated along the horizontal coordinate (centigrade at the

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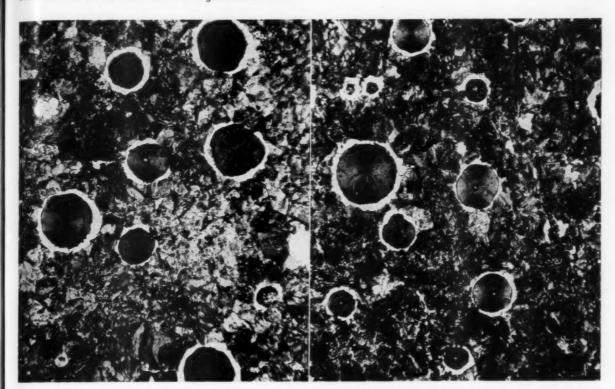
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Cycle I-(a) Heating to 1000 C (1830 F) at 8.3 C/min (15.2 F/min). (b) Cooling to R.T. at 50 C/min (90 F/min).

Fig. 7-Microstructure of 1-in. section. Nital etch; x 400.

Fig. 8-Microstructure of 1/4-in. section. Nital etch; x 400.

bottom and fahrenheit at the top) and linear dimensional changes in millimeters along the vertical axis. The latter changes are caused by thermal expansion or contraction plus permanent growth, magnified about 200 times as compared to the actual dimensional changes of the sample. The permanent growth can be directly measured from the distance between the starting point of the heating branch and the final point of the cooling branch of each curve along the vertical coordinate.

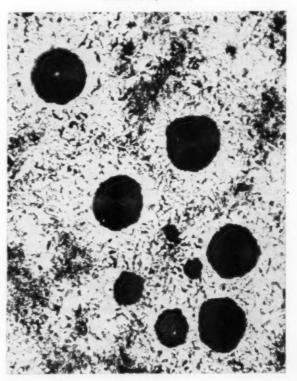
Moreover, each dilatometer sample, after the completion of the cycle, was micro-examined and some of them were photographed. Representative structures presented might be compared with Fig. 2, 3, 4 and 5, i.e. with their original as-cast structures.

Cycle 1 (Fig. 6): (a) heating to 1000 C (1830 F) in 2 hr (8.3 C/min or 15.2 F/min)

(b) cooling to room temperature "in air" (50 C/min or 90 F/min)

In this case, each sample was purposely heated to a high temperature and then rapidly cooled, without giving time for pearlitic carbides to graphitize. It might be noted that growth commences and progresses rather rapidly after the sample is heated into the temperature range of over 1292 F (700 C).

Degree of growth varies for different samples, and depends on the amount of the original primary cementite. In samples A, D and G (1-in., ½-in. and %-in. sections, respectively) it is small (about 0.2 per cent). In sample J (¼-in. section) it is slightly higher (0.25 per cent). Finally, in sample M (½-in.



Cycle 2

Fig. 10-Microstructure of 1-in. section.

Nital etch; x 400.

section) it is quite high (0.87 per cent).

Resultant microstructures are represented by Fig. 7, for 1-in., 1/2-in. and 3/8-in. sections, and by Fig. 8, for 1/4-in. and 1/8-in. sections. The two structures are practically identical, except that the latter displays small amounts of ungraphitized primary cementite which are not present in the former.

In other words, this cycle tends to eliminate the structural differences between heavy and light sections and makes their hardnesses (Fig. 6) more uniform. It does not increase the amount of free ferrite and in some cases might even reduce it (compare Fig. 7 with 2). Likewise, it does not make cast-

Cycle 2 (Fig. 9): (a) heating to 900 C (1650 F) in 2 hr (8.3 C/min or 15.2 F/min)

(b) holding at 900 C (1650 F) for

1 hr

(c) cooling to room temperature "in air" (50 C/min or 90 F/min)

(d) heating to 750 C (1380 F) at

8.3 C/min (15.2 F/min)

(e) holding at 750 C (1380 F) for

1 hr

(f) cooling to room temperature "in air" (50 C/min or 90 F/min)

This cycle represents a double heat treatment, "normalizing" at 1650 F (900 C) and "drawing" at 1380 F (750 C). At each one of these two temperatures, the dilatometer sample was held for 1 hr. The dilatometer cycle of each sample is, thus, represented (Fig. 9) by two thermal curves.

It might be noted that in the case of 1-in., ½-in., ½-in., and ¼-in. sections (Sample No. C, F, I, and L) all of the growth due to the graphitization of primary cementite ("normalizing" curves) occurred upon heating to 1650 F (900 C) and very little, if any, upon holding at this temperature. In the ½-in. section (Sample No. 0) some additional growth took place during the 1-hr exposure to 1650 F (900 C). This is due to higher percentage of primary cementite in the original metal. Even though not microexamined in this condition, the structures of these samples, after the normalizing step, should have been comparable to Fig. 7 and 8.

The object of the second or drawing step was to graphitize the pearlitic carbides. Referring to Fig. 9, all of it occurred during the 1-hr exposure to 1380 F (750 C).

The percentages of total growth in various samples (Fig. 9) were greater than the corresponding ones after normalizing only (Fig. 6), which is an indication of more complete graphitization in the present case. Nevertheless, as can be seen in Fig. 10, which is representative of all five samples of the present cycle, some combined carbon in the form of pearlite still remained.

Referring to the "drawing" curves of Fig. 9, it might be noted that on the cooling branch of each curve a partial transformation takes place at about 1070 F (580 C). This means that at the beginning of cooling from the "drawing" temperature (1380 F or 750 C) each sample was partially austenitic. This in turn explains the presence in Fig. 10 of small pearlitic

grains which were formed from the austenitic phase upon cooling from the "drawing" temperature.

In sample No. 0 (and perhaps L) small patches of primary cementite could also be seen, mixed up with pearlite.

Cycle 3 (Fig. 11): (a) heating to 900 C (1650 F) at 8.3 C/min (15.2 F/min)

/min)
(b) holding at 900 C (1650 F)

for 1 hr

(c) cooling to 700 C (1290 F) at

3.2 F/min

(d) holding at 700 C (1290 F)

for 1 hr

(e) cooling to 650 C (1200 F)

at 3.2 C/min (5.8 F/min)

(f) cooling to room temperature

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"in air" (50 C/min or 90 F/min)

This cycle simulates annealing from 1650 F (900 C) with 1-hr arrest at 1290 F (700 C). Its results in general are similar to those of Cycle 2, except that in this case two steps are combined in one, without intermediate cooling to room temperature. The discussion of Cycle 2 may thus, in general, apply equally

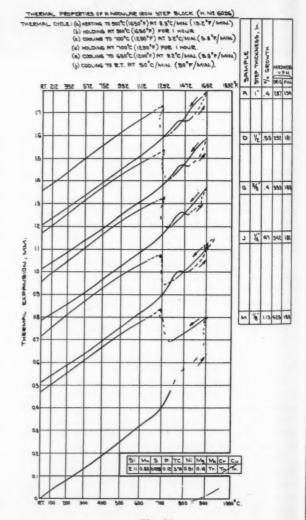


Fig. 11

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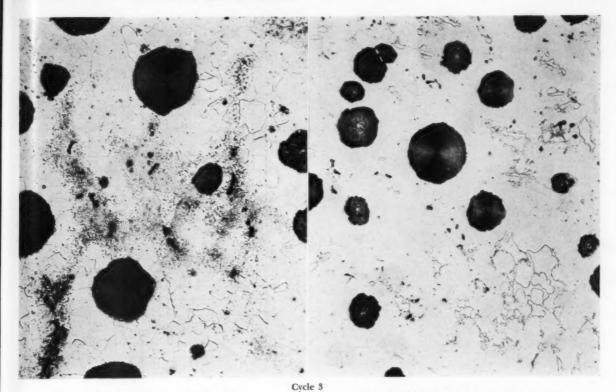


Fig. 12-Microstructure of 1-in. section. Nital etch; x 400.

Fig. 13-Microstructure of ½-in. section. Nital etch; x 400.

well to the present case. Corresponding percentages of total growth, resulting from these two cycles (Fig. 9 and 11) likewise, are about the same.

Microexamination revealed that graphitization of pearlite in the present case progressed somewhat further, but still was not quite complete. In the samples from 1-in., ½-in. and 3/8-in. sections (A, D and G, Fig. 11) some residual pearlite could be seen (Fig. 12). In the samples from ½-in. and ½-in. sections (J and M, Fig. 11) pearlite disappeared almost completely, but residual primary cementite was quite conspicuous (Fig. 13). More complete graphitization of pearlite, as compared to Cycle 2, should be attributed to slow cooling over the entire temperature range from 1650 F (900 C) to 1200 F (650 C).

Dilatometer experiments described so far have definitely indicated that (a) either the temperature of 1650 F (900 C) was, perhaps, not quite high enough, or 1-hr exposure not long enough to graphitize primary cementite and (b) holding within the transformation range should be prolonged and split into several steps in order to graphitize pearlite more completely.

Hence, several dilatometer cycles, outlined in accordance with the above observations, have been tried on single samples.

For example, Cycle 4 (Fig. 14): (a) heating to 925 C (1700 F) at 8.3 C/min (15.2 F/min)

(b) holding to 925 C

(1700 F) for 3/4 hr (c) cooling to 725 C (1335 F) at 3. 2 C/min (5.8 F/min)

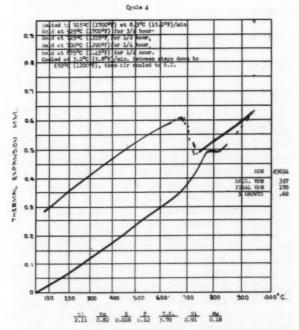
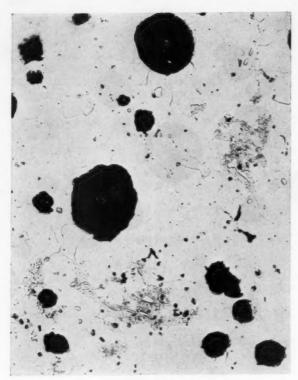


Fig. 14-Sample L-1/4-in. Section from Nodular Iron Step Black (Heat 6096).

(d) holding at 725 C (1335 F) for ½ hr (e) cooling to 700 C (1290 F) at 3.2 C/min (5.8 F/min)



Cycle 4
Fig. 15—Microstructure of 1/4-in. section.
Nital etch; x 400.

(f) holding at 700 C

(1290 F) for 1/4 hr

(g) cooling to 675 C

(1245 F) at 3.2 C/min (5.8 F/min)

(h) holding at 675 C

(1245 F) for 1/2 hr

(i) cooling to 650 C

(1200 F) at 3.2 C/min (5.8 F/min)

in)

(j) cooling to room temperature "in air" at 50 C/min (90 F/min) was performed on a sample taken from a ½-in. section. It is comparable to Cycle 3, except that the temperature of the first step was raised somewhat and its time shortened. Likewise, the second step was spread over several temperatures within the transformation range and its time lengthened.

The resultant dilatometer curve is shown in Fig. 14* and the microstructure of the sample in Fig. 15.

From Fig. 14 it is apparent that each step at progressively lower temperatures within the transformation range was associated with some additional growth (i.e. graphitization) which was particularly rapid at about 1290 F (700 C). The microexamination (Fig. 15), however, revealed that after this cycle, the sample still contained small amounts of residual pearlite and primary cementite. This indicates that time periods allowed to both (a) high temperature and (b) transformation temperature graphitizing steps, still were insufficient.

Finally, Cycle 5 (Fig. 16): (a) heating to 950 C (1740 F) at 8.3 C/min (15.2 F/min)

(b) holding at 950 C

(1740 F) for 2 hrs

(c) cooling to 725 C

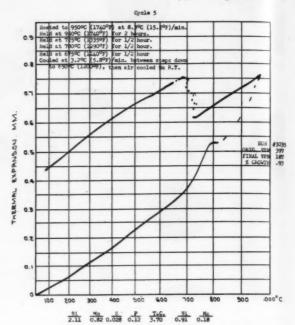
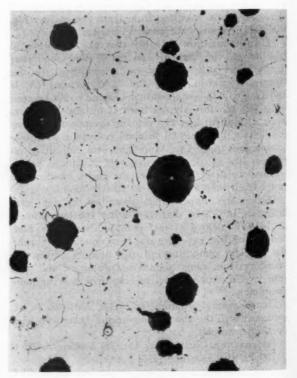


Fig. 16-Sample O-1/8·in. Section from Nodular Iron Step Black (Heat 6096).



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Cycle 5

Fig. 17-Microstructure of 1/8-in. section.

Nital etch; x 400.

[•] It should be noted that while Fig. 6, 9 and 11 are reproductions of the tracings of the original curves, Fig. 14 and 16 are reproductions of the original photographically recorded curves.

C

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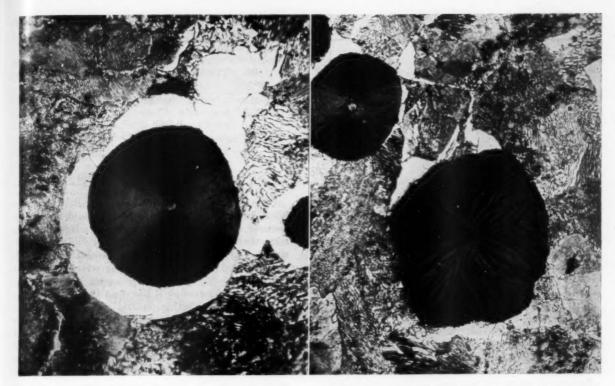


Fig. 18—Smooth appearing nodule with center nucleus. %-in. section of step block, as-cast. Nital etch, x 1000.

Fig. 19—Pronounced radial bands in nodule. 3/6-in. section of step block, as-cast. Nital etch, x 1000.

| (1335 F) at 3.2 C/min | (5.8 F/min) | | |
|-----------------------|----------------|-----|---|
| | (d) holding at | 725 | C |
| (1335 F) for 1/2 hr | | | |
| | (e) cooling to | 700 | C |
| (1290 F) at 3.2 C/min | | | |
| | (f) holding at | 700 | C |
| (1290 F) for 1/2 hr | | | |

(g) cooling to 675 C (1240 F) at 3.2 C/min (5.8 F/min)

(h) holding at 675 C (1240 F) for ½ hr

(i) cooling to 650 C (1200 F) at 3.2 C/min (5.8 F/min)

(j) cooling to room temperature at "in air" 50 C/min (90 F/min) performed on a sample from the ½-in. section produced, as can be seen in Fig. 17, a structure practically free from combined carbon. Elimination of primary cementite was, no doubt, due to the longer time period and somewhat higher temperature of the first step, while elimination of pearlite was caused by the longer time within the transformation range and also by carrying this second step to a lower temperature than in Cycles 1 to 3.

Similar or slightly modified cycles have been applied to samples taken from heavier sections. In general, they respond similarly to the observations noted from Cycles 4 and 5, except that their total growth (due to lower original combined carbon) was less than that indicated in Fig. 14 and 16. However, the residual pearlite in thicker sections appears to be somewhat more sluggish and more difficult to

be completely graphitized, than that in the thin sections.

Nevertheless, Cycle 5 was sufficient to produce complete graphitization and a structure similar to that of Fig. 17 in the sections up to 3/8-in. In thicker sections slight amounts of the residual fine pearlite still could be seen.

An attempt was made to apply various heat treatments, as indicated by the results of dilatometer studies, to test bars and to investigate how they affect the physical properties. Hence, a set of eight standard steel-type tensile test bars was machined from a coupon of the following composition:

These bars were tested in pairs, as-cast and heat treated as per schedules of dilatometer cycles 2, 3 and 5. In other words, the heat treatments consisted of (a) normalizing and drawing and (b) two "step-cycleannealings," one more rapid, and the other slower and conducted from somewhat higher temperature. All broken test bars were microexamined.

Results of these tests are given in Table 2. From them one might judge how heat treating makes nodular iron progressively softer and more ductile.

Summary

By observing certain rules in selecting the base metal and its treatment with magnesium, nodular iron of high strength and ductility can be produced con-

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Table 2—Physical Properties of 0.505-in. Diam, 2-in. Gage Machined Test Bars, Heat Treated According to Different Dilatometer Cycles

| Serial No. | T.S., psi | Y.P., psi | P.L., psi | B.S., psi | El., | R.A., | Me., psi x 10° | V.P.N. | Heat Treatment Comparable to the Dilatometer Cycle No. | Microstro Compa to Fig. | rable |
|---------------|--------------|--------------|--------------|--------------|------|-------|-------------------|--------|---|-------------------------------|-------|
| 1 | 94,000 | 86,000 | 59,000 | 95,000 | 1.5 | 1.0 | 23.3 | 190 | As cast | Fig. | 2 |
| | 97,000 | 84,500 | 62,000 | 98,000 | 1.5 | 1.0 | 22.6 | | | - | |
| 2 | 94,500 | 69,000 | 64,000 | 96,900 | 4.0 | 2.5 | 24.4 | | Cycle No. 2 | Fig. | 10 |
| | 91,000 | 67,000 | 49,000 | 95,200 | 5.0 | 4.5 | 20.7 | | (normalizing and draw | ring) | |
| 3 | 76,000 | 57,000 | 42,000 | 87,400 | 15.5 | 13.0 | 21.4 | 185 | Cycle No. 3 (faster | Fig. | 12 |
| | 74,500 | 55,500 | 41,000 | 83,700 | 14.0 | 11.0 | 21.0 | | "step-cycle-annealing" |) | |
| 4 | 70,500 | 50,500 | 36,000 | 82,000 | 16.5 | 14.0 | 25.0 | 172 | Cycle No. 5 (slower | Fig. | 15 |
| | 71,000 | 51,500 | 42,000 | 82,600 | 15 | 14.0 | 20.4 | | "step-cycle-annealing" |) | |

sistently

Nodular iron in cross-sections of about ½ in. and up to about 3 in. is not sensitive to the wall thickness. In thinner cross-sections, however, it rapidly becomes white, hard and brittle, thus necessitating heat treating. This is particularly true for thin walled castings.

The present paper describes a series of dilatometric studies of thermal properties of nodular iron samples taken from a casting composed of progressively thicker cross-sections.

It has been found that the permanent growth produced by heat treating, and associated with complete (or almost complete) graphitization of combined carbon, in cross-sections of over ½ in. is uniform and amounts to about 0.5 per cent of linear dimensions. In thinner cross-sections it rapidly increases and in those of about ½ in. thick becomes as high as 1 per cent or over.

Complete or almost complete graphitization of combined carbon is best achieved by a "step-cycle annealing" treatment from 1740 F (950 C). At least 2 hr holding period at 1740 F (950 C) is necessary to graphitize the primary cementite.

It should be followed by about 1½-hr holding period in the range of 1335 to 1155 F (725 to 625 C), to graphitize pearlitic carbides. Graphitization of pearlite progresses most rapidly upon its formation from austenite. Hence, holding period within the above range should preferably be divided into three equal steps at temperatures of 1335, 1290 and 1240 F (725, 700 and 675 C). In thin-walled castings this second step results in almost complete graphitization of pearlite. In heavier-walled castings graphitization of pearlite appears to be more sluggish, which might necessitate longer time periods within the above temperature range.

If some excess primary cementite remains after the high temperature holding period, it is difficult to graphitize in the transformation range.

Hence, heat treating of nodular iron, at least to some extent, should be regulated according to the casting size. Small, thin-walled castings should be allowed longer time at the high temperature and require less time within the transformation range. Heavy-walled castings, containing little or no excess carbides, require less time at high temperatures. In some cases, the high temperature heat treating step for the thick-walled castings might be completely omitted. But they should be allowed ample time within the transformation range.

For castings composed of heavy and light sections, selection of the heat treating cycle should be a compromise of the above considerations. It should be remembered that permanent growth, due to graphitization, in such castings will unavoidably be non-uniform and might result in a certain amount of distortion.

The above discussion is based on the consideration of maximum graphitization and maximum ductility in the final product. For the purpose of improving other properties, such as hardness, strength and yield point, the heat treating cycle should be modified accordingly. It should be remembered that all present heat treating experiments were performed on very small samples. In heat treating commercial castings corrections for the mass action should be introduced.

Generally speaking, heat treating of thin-walled castings is more important and critical than that of the heavy-walled ones. It might be that the latter, at least in some instances, need not be heat treated at all. However, it appears that, for the purpose of uniformity, all nodular iron castings should be subjected to some heat treatment.

All present discussions refer to a nodular iron of a definite composition. When the composition of the metal is changed, various heat treating steps should be changed accordingly, but the fundamental considerations remain the same.

Addendum

Several investigators have reported that some interesting structures can be observed within the graphite of "as cast" and heat treated nodular cast irons. From the photomicrographs in the paper, it may be noted that all of the nodules appear to be of the spherulitic type described by Morrogh.9 Many of them have an off-white hard-appearing central nucleus, from which bands of graphite crystallites radiate out to the spherical perimeter. This nucleus seems to be the same as numerous fine inclusions scattered throughout the matrix and may be composed of magnesium carbide, sulphide or perhaps a more complex compound. Many inclusions are frequently found in gray and malleable cast irons, however, they have a different appearance. Thus those observed in nodular iron may be assumed to have resulted from the magnesium addition. These particles appear to be brittle and fragile, so they are frequently dragged out in polishing.

When the nodule has been sectioned through the center (Fig. 2, 3 and 18), the radial bands are usually

faint and the graphite appears hard packed and shiny. In some cases faint annular-like rings may be observed (Fig. 2). Other nodules probably sectioned off center do not show a definite nucleus, but have much more pronounced radial bands as illustrated in Fig. 19.

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The graphite in some of the nodules appears softer and is more difficult to preserve intact during polishing as indicated in Fig. 4 and 5. Some nodules have a flaky aggregate appearance like those found in malleable iron, however, it is believed this is due to a polishing effect rather than to the method of crystalliza-

Heat treatments used to decompose the primary cementite and pearlite have had little effect on the appearance of the nodules. Evidently the new graphite deposited on the original nodules in the same radial and sometimes "annular" ring pattern (Fig. 13).

The type of graphite formation, whether as a flaky aggregate or spherulitic nodules, probably has little if any effect on the mechanical properties, as they are determined by the metal surrounding them.

Acknowledgment

The authors wish to express their thanks to Crane Co. for permission to publish this work, to Messrs. A. F. Lahr, H. A. Peterson and B. E. Jenkins for their assistance and to the Control Laboratory for the chemical analysis.

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DISCUSSION

Chairman: R. G. McElwee, Vanadium Corp. of America, Detroit

Co-Chairman: V. A. CROSBY, Climax Molybdenum Co., Detroit. J. E. REHDER (Written Discussion): 1 This account of a wellconducted research into the metallurgy of annealing nodular iron is welcome, and embodies further evidence that there is much similarity in the graphitization of nodular irons, malleable irons, and white cast irons. In many respects the writer is in complete agreement with the authors, but there are some points that would seem to bear discussion.

In the opening paragraphs of the paper, the statement is made that phosphorus is not affected by magnesium. The present writer cannot agree that the interaction between phosphorus and magnesium is negligible, since work in progress at the Canadian Bureau of Mines shows definitely that magnesium can act as a dephesphorizer, and that iron of increasing phosphorus content requires increasing minimum amounts of residual magnesium for nodular structure. Furthermore, the dark, irregular inclusions that accompany persistent pearlite in annealed nodular iron are suspected of being a magnesium-iron-phosphide com-

It might be noted that the magnesium contents of two of the heats mentioned in the paper (No. 6095 and 6096) are unusually

high for such light-sectioned castings as used. Among the data recorded are the Vickers hardness numbers of the samples as cast. Considerable variation is noted on samples apparently from the same location, and also it is surprising to find the as cast hardness of the steps from Heat No. 6096 lower in every case than the steps from Heat No. 6095. Since the latter heat has a silicon content nearly 0.50 per cent higher, these hardnesses are in reverse order to that expected. In this connection, we find that Vickers and Rockwell "C" hardness measurements are not trustworthy on nodular iron unless many readings are taken and averaged. This is believed due to the small size of these diamond indenters and the small area of sample covered in a heterogeneous material like nodular iron.

The writer agrees with the authors with respect to Fig. 9 and 10, in that the 1380 F holding temperature used was definitely above the lower critical temperature in the mixed austeniteferrite range. Throughout this work, the cooling rates through the critical temperature range have been rapid (190 F per hr) and thermal arrests have been depressed considerably.

Mention is made several times in the paper that the 1/2-in. thick sample graphitized more rapidly in the critical and subcritical temperature regions than did the thicker sections. This is to be expected, as it is well known that graphitization rates of malleable iron and white cast iron depend heavily and quantitatively on as-cast section size, (Schneidewind et al, A.F.S. TRANSACTIONS 1948 and 1949), and there is no reason for supposing nodular iron to act differently. It might be noted that whereas Cycle 5 was more successful in eliminating pearlife than was Cycle 4, the former was used on a sample of smaller as-cast size which would graphitize more rapidly anyway.

In drawing conclusions as to suitable commercial annealing cycles for nodular cast iron, it should be kept in mind that the arrests shown in Fig. 6, 9, 11, 14 and 16 are low due to relatively rapid cooling rates used, and that graphitization shown in the paper, is, in the opinion of the writer, largely subcritical. Subcritical annealing is almost invariably longer in total time required than is second-stage graphitization by slow cooling through the true critical temperature range.

The authors are to be thanked especially for the exhaustive bibliography on the subject of nodular iron which is appended to their paper. This is the first extensive list the writer has seen published.

A. I. KRYNITSKY (Written Discussion): 2 The authors should be congratulated with their excellent presentation. I can offer only a few minor comments on this valuable contribution to the study of nodular iron.

It was of an interest to learn that the nodular irons containing between 0.5 and 1 per cent manganese possessed the higher physical properties than those containing lower percentages of this element.

In our investigation special attention was given to the effect of manganese on the formation of globular graphite. However, the results obtained thus far do not warrant any definite con-

On page 637, column 2, the authors give the physical properties of their nodular iron and say that they can consistently produce these properties. This statement, however, seems to contradict their other statement that "The above physical properties, as measured by 0.505 in. in diam, 2-in. gage bar machined from a steel-type coupon block, sometimes can be obtained even in ascast condition.

Does it mean that the above properties can be obtained consistently only in a heat-treated condition and not in as-cast condition?

In the first paragraph on page 642, the authors compare the microstructures of 1-in., 1/2-in. and 3/8-in. sections and conclude that the two structures are practically identical, except that the structures of 1/4-in. and 3/8-in. sections displays small amounts of ungraphitized primary cementite which are not present in the heavier sections.

It is suggested that the authors explain why the samples J and M whose structure displays small amounts of primary cementite are softer than sample A which is free from this hard constituent.

On page 645 (at the bottom, column 1) the authors write: "However, the residual pearlite in thicker sections appears to be somewhat more sluggish and more difficult to be completely graphitized, than in the thin section."

Is this sluggishness of the pearlite a function of the quantity or fineness or both of pearlite present at time of heat treatment? The authors' bibliography is very extensive and has considerable reader value.

CO-CHAIRMAN CROSBY: The authors made the statement: "However, it appears that for the purpose of uniformity all nodular iron castings should be subjected to some heat treatment." This is a significant statement and one that will cause the proponents of pearlitic malleable to question the feasibility of nodular iron particularly in medium and small sections.

J. V. Olle: 3 In reference to heat treatment of nodular cast iron, has it been definitely established that heating to 1650 or 1700 F then through a slow cooling range maximum free ferrite will be obtained or is it absolutely necessary that a temperature arrest be carried out at perhaps 1275 to 1300 F?

Mr. Goldsmith: As previously pointed out, this is for one particular composition. Undoubtedly most people complain that

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Metallurgist, National Bureau of Standards, Washington, D. C.
 Metallurgist, Motor Castings Co., West Allis, Wis.

the manganese is too high, but for this one composition with a higher manganese, which results in a more pearlitic structure, I do not see how in the various sections you can get away without arresting in the transformation range. Perhaps with the lower manganese you will get the more ferritic structure and thus be able to get by without arresting at that temperature, or perhaps eliminate the high temperature part of the cycle.

In answer to Mr. Rehder's discussion as to the comments on lowering phosphorus content, the only thing we can say is that we are glad to see this result. It is quite startling. We at no time encountered this fact and in private talks with Mr. Rehder, we find he is somewhat dubious about what to do with it. He is doing more work on the problem and we would be very interested in the results. We have no information on the lowering of

phosphorus with the magnesium.

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As to taking the hardness with the Vickers machine, we agree that you must be careful in measuring the hardness of the cast iron, but there is no other tool we can use in this study because our thinner sections cracked. We tried it on the machined dilatometer bars and the size prohibited any other measurements. We do feel that there is a general trend in the correlation of experimental results and it does show a qualitative evaluation of what we can expect with the hardness measurements used. We cannot explain why the hardnesses as he pointed out varied, except to say that the recorded values are within experimental error. We are not in full accord with his remarks about this being a fast cooling rate. He quotes 190 F per hr. It is true that between our various stages of holding that we did cool it at fast rates, but we were holding these bars in the transformation range which I do not believe suppressed the transformation, nor do I believe that we are cooling it too fast.

I take issue with him in his paper "An Introduction to the Annealing of Nodular Iron" (see p. 298) where he mentions taking castings out of the furnace when the temperature was about 1300 or 1330 F. I believe that is a bit high to achieve the maximum ductility. We annealed some test castings with extremely thin section sizes (1/8 in.) and by applying Cycle 5 we achieved complete graphitization. In other words, in some castings you do have to make a correction for mass as compared to the very small dilatometer bar, but generally speaking, for light castings I do not think we are too far off in our above-mentioned cycles. We will naturally have to make a correction on some of them but in this thin-section casting it can be fairly well annealed

by following this cycle.

W. W. Levi: 4 Mr. Goldsmith made some comments on dephosphorization of cast iron resulting from the treatment with magnesium. In connection with some of our early experimental work involving the manufacture of ductile iron pipe we too, felt that the magnesium treatment actually removed some phosphorus, but quite soon thereafter found that we were mistaken. One of the irons used was found to contain 0.56 P before addition of the magnesium-bearing alloy and after treatment it

apparently contained only 0.38 P.

Samples of the above-mentioned irons taken from annealed pipe were sent to another laboratory for checking and this laboratory found the phosphorus content to be 0.55 P which for all practical purposes is the same as found by our laboratory on samples taken from the iron before treatment with magnesium. The annealing cycle involved is one where pipe is heated to a temperature of 1700 to 1710 F for a period of about 12 min followed by slow cooling. The entire annealing cycle requires about 72 min. Our laboratory then annealed some of the samples which apparently contained only 0.38 P. After annealing, results

Co-Author Ziegler: I would like to point out once more that the main object of this experiment was to find out the response to nodulization of sections of different thicknesses. Our work shows that thin-walled castings, even though partly nodu-lar, contain so much excess cementite, that as-cast are quite hard. Our paper indicates several heat treating cycles, by means of which such castings can be made soft and ductile. Assume now another extreme, namely, a casting with, perhaps, 1-in. thick wall. It is quite possible that it might be sufficiently soft and ductile even as-cast and will not require any heat treating. Now, suppose that we have a casting in which the wall thickness varies from, perhaps, 1/8 to 1 in. It must be heat treated, in order to soften thin parts. On the other hand, as our paper points out, permanent growth which is a function of graphitization of combined carbon during heat treating, will be different in different cross-sections. If this is the case, it becomes apparent that a casting with a nonuniform wall thickness, when heat treated, might badly warp. To avoid warping, the amount of combined carbon in thick and thin sections should be kept constant, which is not an easy matter to achieve.

MR. GOLDSMITH (Authors' Closure): The authors wish to express their thanks to all those who have contributed to the discussion of their paper. Replying to Mr. Krynitsky's discussion, remarks on page 637 were intended to be a general statement of the representative physical properties of nodular iron. The wide range was intended to take into account chemical analysis, heat treatment and section thickness. The remark on page 638 was intended to show what could be obtained in the as-cast condition with the proper chemical analysis, and sufficient crosssection. However, it has been the authors' experience that more consistent results can be obtained on heat treated bars and much closer control is needed to get consistent as-cast results.

It is well known that in any cast iron, and particularly in the nodular, physical properties to a considerable extent are determined by the amount of combined carbon. This factor is much easier to control by a suitable heat treatment than by policing the manufacturing techniques. In fact it has been the authors experience that more consistent results can be obtained by making nodular iron either completely pearlitic or even slightly on the "white" side and then subjecting it to a short heat treatment, rather than by trying to make it ferritic as-cast.

As stated in the oral reply to Mr. Rehder's discussion, regarding inconsistencies in the hardness measurements, Vickers impression occupies so small an area that it might miss small particles of excess cementite found in a heat treated thin section. On the other hand, in a heat treated heavy section, if there is any combined carbon left, it usually appears as pearlite. The latter, for the same percentage of combined carbon, will occupy a much larger volume than if it were present as excess cementite. For this reason there is more chance that it will be detected by Vickers identation, thus registering higher hardness.

It is our experience that graphitization of combined carbon in nodular iron during heat treating is by no means a simple phenomenon. Briefly, pearlite in heavy cross-section frequently appears to be more sluggish than when it is associated with excess cementite, as might be expected in a thin section. Consequently, after a given heat treatment a heavy cross-section might contain more combined carbon than a thinner one.

of chemical analysis indicated the presence of 0.57 P. From this it seems likely that annealing magnesium-treated irons brings about a change which enables us to detect practically all of the phosphorus present, while this is not the case with unannealed samples by the method of analysis used. In view of the above, it is very doubtful that treating cast iron with magnesium alloys brings about any reduction in phosphorus content.

⁴ Metallurgist, Lynchburg Foundry Co., Radford, Va.

FLOWABILITY OF MOLDING SANDS

By

William H. Moore*

THE CONCEPTION OF FLOWABILITY OF MOLDING SANDS is not a new one. In 1934, Dietert proposed the sand movement method in a paper read before the A.F.S. Basically his method consists in measuring the amount of compaction in an A.F.S. specimen between the fourth and fifth rams on the standard A.F.S. rammer. His degree of ramming was chosen arbitrarily after much experimentation. He obtains the percentage flowability from the following formula:—

% Flowability = $100 - (100 \times \text{compaction in inches})$

In 1940, Kyle in another paper before the A.F.S. proposed an improved conception of flowability which depended on hardness measurements the basis being that uniform hardness results from good flowability. Two basic conceptions were outlined, viz., the Hardness Gradient Method and the Hardness Differential Method. In this latter method a standard A.F.S. specimen holder split down the center is used and the flowability is expressed as follows:—

% Flowability = Hardness at top + Hardness at Bottom

The utilization of bulk density in expressing flowability has also received consideration by Buchanan in the *Foundry Trade Journal* of 1933 and by Ash and Lissel in the *Foundry*, 1941, but it may be stated that the Dietert and the Kyle methods have been accepted more generally.

If we compare these two methods (Dietert and Kyle) by measuring the variation in flowability with moisture on a New Jersey 125 sand it may be seen that this variation is almost exactly opposite with the two methods.

To more closely appreciate the function flowability it behoves us to consider the definitions advanced by both Dietert and Kyle.

Dietert—"Flowability is defined as the property which enables sand to flow when a ramming energy is applied. The greater the ease of flowing, the more readily will the sand form a continuous and uniform mold surface."

Kyle—"Flowability as applied to molding sands should indicate the property which permits sands to be rammed, squeezed or jolted, around a pattern in such a way that the energy applied causes the sand to surround the pattern completely and be at a suitable and uniform hardness at the pattern surface without producing undesirable hardness conditions in other regions of the mold."

Both definitions evidently require a uniform condition over the mold surface and both recognize the ability of a sand to flow. The method of measurement and interpretation which will be outlined in this paper involves the ability of a sand to give uniform properties, viz., strength and permeability over the mold surface.

It is recognized at the outset that ability to give uniform properties is only one phase of flowability and that the second phase, viz., the energy required to cause a sand to flow by overcoming the viscosity of the sand, is also an important one. However, in order to introduce a flowability value based on test results it has been assumed that sufficient ramming energy

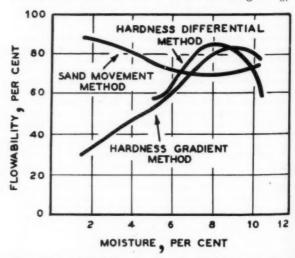


Fig. 1-Relationship of moisture content and flowability of N. J. No. 125 sand.

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will always be available to overcome the natural viscosity of a sand. Consequently any reference to flowability which follows concerns itself only with mold properties and not with energy required to ram or work the sand.

The properties we are primarily concerned with are undoubtedly strength and permeability because strength is required for shaping the sand and for resisting the fluid pressure of the iron, whereas permeability is required to allow grain movement and for evolution of gases produced during casting. It is proposed that these two properties, viz., permeability and strength are divided into two distinct designations called for convenience ventability and bondability. This leads us to the following definition:—

"Sand flowability may be regarded as the ability of the sand grains to move relative to one another when sufficient energy is applied, thereby presenting a mold surface of uniform permeability and uniform strength. The degree of uniformity of permeability may be expressed as the *ventability*, and the degree of uniformity of strength may be expressed as the *bondability*.

To arrive at a method of measuring *ventability* and *bondability* it is necessary to arrive at some conception of what an ideal sand would be.

Ventability

An ideal sand would be one that packed together in a constant degree, thereby presenting constant void spaces between the sand grains regardless of the degree of ramming.

Consider each sand grain as a uniform sphere and every sand grain of uniform size with smooth surfaces, as in ball bearings, so that there is no friction offered to the movement of these sand grains. If a large number of these grains were poured into a given space, they would automatically assume a uniform close

IDEAL PACKING

LESS IDEAL CONDITION

Fig. 2-Representation of ideal and less ideal packing of sand grains.

packing as in Fig. 2. No ramming or pressure applied would make these grains pack together more closely. We have in fact an ideal flowability. We have also an equal number of void spaces between the grains at all times, and we have therefore a *ventability* of 100 per cent.

If now we included with these uniform diameter spherical grains sufficient uniform small size grains to fill in the spaces between the larger spheres, we would have an entirely different condition. Here, no matter how long we jolted or agitated the grains, we would probably never assume the exact mathematical arrangement which would give the closest packing of the grains and therefore a constant amount of void space between the grains. As long as the sand grains cannot be readily packed together in their closest arrangement, we will always have some variation in void space and in ventability over the whole surface occupied by the grains. We would in fact have a low ventability—low from the standpoint of uniformity which forms our basis of flowability.

It is evident from these considerations that the ventability depends largely on the grain distribution and the grain shape. The actual overall size of the grain is not as important as the relative size of each group of grains to the other. Thus, a uniform fine grain would be expected to show a good ventability and uniformly coarse grain would be expected to show a good ventability. In the same way we would expect rounded grains of any given distribution to show better ventability than subangular or angular grains of the same distribution, because the rounded grains would more readily flow together and assume the condition of closest packing.

If now we take any sand and ram it in successive stages of increase of ramming hardness, we would expect successive changes in the closeness of packing. This closeness of packing may be measured by measuring the bulk density of the sand at different levels or more readily yet by measuring the permeability. By measuring the permeability we obtain an average value of the void space existing throughout a given sand sample.

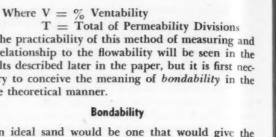
The basis of measuring and calculating ventability becomes the measurement of the permeability of a sand sample after each ram on the A.F.S. standard rammer, the rams being conducted in one ram increments from an initial one ram to a final of ten rams.

Consider a typical result obtained from a medium green sand natural bonded facing and shown in graphical form.

In an ideal sand the first ram would result in a given permeability or closeness of packing, and subsequent rams would not alter this permeability from its initial value as the sand was packed as closely as possible after the first ram.

If this initial value was, say, 60, then in the ideal sand all the succeeding values would be 60. If then we divide each permeability value by the succeeding value, we would have 60 and so on. As 60 is equal to

1.00 we would have nine subtotals of 1.00 as there are nine increments of ramming. Adding these subtotals together would result in a total ventability of 9.00 in



The practicability of this method of measuring and its relationship to the flowability will be seen in the results described later in the paper, but it is first necessary to conceive the meaning of bondability in the same theoretical manner.

An ideal sand would be one that would give the greatest strength uniformity over the mold surface with the minimum degree of ramming. Going back to the conception of equal size rounded grains, it is necessary to imagine each grain surrounded with a uniformly

sider as the clay bond. The most acceptable theory of clay as a binding material postulates alternate layers of clay flakes and water flakes, the exact arrangement depending on the clay type involved. Thus, three types of bond struc-

thick layer of bonding material which we will con-

ture may be distinguished.

1. Montmorillonite Group and Secondary Mica Group-In the water film between two flakes water dipoles are interposed between the surface cations and the surfaces of the flakes. Assuming water dipoles arranged in layers, these layers will carry a positive charge on one face and a negative charge on the other. As quartz has a negative surface charge, the structure of the water film between a clay flake and the quartz grain resembles that of the water film between two clay flakes. However, there are not so many cations as these are derived from the clay flakes alone. Thus, the intensity of the electrostatic field between the quartz grain and a clay flake is not as high as that between two clay flakes. Consequently, clay flakes are not held so tightly to quartz grains as to each other.

2. Kaolin Group-Here the flakes are held together by secondary attractive forces between the hydroxyl sheet of one unit and the oxygen sheet of the next. Here the structure of the film between a clay flake and a quartz grain will be similar to that between two

flakes.

3. Limonite Group-Cleavage of limonite materials into flakes does not occur as readily as in clay materials. As the limonite materials have a positive charge and the quartz has a negative charge, there will be an intense electrostatic field between the two, and they will be tightly held together.

As these clay bonds vary somewhat in their structure, we would expect some effect on bondability according to whether the clay flakes are strongly attracted to the grains and to each other or according to whether they are loosely attracted and will not interfere with the movement of the sand grains.

As also the binding force is electrostatic in nature, the closer the approach between the grains and the flakes, the stronger and the more uniform will the bond. Thus, hard ramming and close packing will result in high and uniform bonding. The degree of ability to pack closely will be determined by the character of the clay bond and the number of water films associated with the clay bond.

Thus, the greater the number of water films, the more their tendency to leave an ordered electrostatic

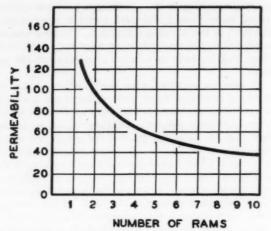


Fig. 3-Relationship of number of rams vs permeability in medium green sand natural bonded. Moisture 5.4%, Ventability 82.5%.

the ideal sand.

In any sand we could go through the same procedure and obtain a total value which could be expressed as a percentage deviation from the ideal value of 9.00. To illustrate the method of calculation, consider the sand already referred to in Fig. 3.

| R Ram No. | P Permeability | | | | S | ub-Total |
|--------------|-------------------|----------|----|-----|---|----------|
| 1 | 165 | P at R1 | = | 165 | = | 1.70 |
| | | P at R2 | | 97 | | |
| 2 | 97 | P at R2 | = | 97 | = | 1.35 |
| | | P at R3 | | 72 | | |
| 3 | 72 | P at R3 | = | 72 | = | 1.10 |
| | | P at R4 | | 65 | | |
| 4 | 65 | P at R4 | = | 65 | = | 1.12 |
| | | P at R5 | | 58 | | |
| 5 | 58 | P at R5 | = | 58 | = | 1.11 |
| | | P at R6 | | 52 | | |
| 6 | 52 | P at R6 | = | 52 | = | 1.06 |
| | | P at R7 | | 48 | | |
| 7 | 48 | P at R7 | = | 48 | = | 1.06 |
| | | P at R8 | | 45 | | |
| 8 | 45 | P at R8 | = | 45 | = | 1.07 |
| | | P at R9 | | 42 | | |
| 9 | 42 | P at R9 | = | 42 | = | 1.02 |
| | | P at R10 | | 40 | | |
| 10 | 40 | P at Tor | AL | | | 10.59 |

We have a total of 10.59 as against a value of 9.00 for an ideal sand.

Now percentage ventability would be:-

% Ventability =
$$\frac{9.00 - (10.59 - 9.00)}{9.00} \times 100$$

= 82.3

or expressing this as a general formula, we have:- $V = 9.00 - (T-9.00) \times 100$

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arrangement and form a random arrangement where the films will act as a lubricant and affect the friction between the grains during the packing or ramming operation.

We could readily deduce the probable effect of these clays on flowability, but this we will leave for specific discussion as we find differences resulting from the tests described in this paper. For the present, it is sufficient to realize that the bond may exert a considerable influence on flowability and that *ventability* is one function of flowability, while *bondability* is the other. Only by considering both can we arrive at an understanding of the different workability found in sands during the ramming operation.

It is necessary always to bear in mind that clay arranged around the silica grains and excess clay in lump form which does not move integrally with each grain are quite different. Thus, excess clay not adequately distributed by mulling around the silica grain will act as a void filler as explained in grain distribution in relation to ventability. The net result will be a loss in ventability or uniformity of permeability at the mold face. If, however, the clay moves as an integral part of each grain, there will be little effect on ventability beyond slowing the flow action by virtue of friction or increasing the flow action by lubrication—this depending on the amount of water associated with the clay.

The method of measuring bondability is essentially the same as that proposed for ventability in that the strength is measured at each ram up to ten rams on the A.F.S. rammer. The method of calculation naturally differs because in one case we are measuring strength increase, whereas in the other we are measuring permeability decrease. An ideal sand would be one that gave the greatest strength uniformity, and, while the actual strength value is naturally important, it is almost impossible to designate an ideal strength value because of the many factors involved.

If we again consider results on a medium green naturally bonded sand at 5.4 per cent moisture, we

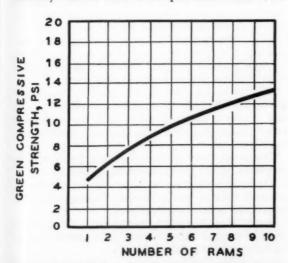


Fig. 4-Relationship of number of rams vs green compressive strength in medium green sand natural honded. Moisture 5.4%, Bondability 87.1%.

have the curve shown in Fig. 4.

If we now set this out as in the ventability test but divide each strength by each preceding one as we are measuring an increase, we have:—

| R Ram No. | S Strength | | | | | Subtotal |
|--------------|---------------|----------|-----|------|---|----------|
| 1 | 4.5 | S at R2 | = | 6.6 | = | 1.46 |
| | | A at R1 | | 4.5 | | |
| 2 | 6.6 | S at R3 | = | 7.8 | = | 1.18 |
| | | S at R2 | | 6.6 | | |
| 3 | 7.8 | S at R4 | = | 8.9 | = | 1.14 |
| | | S at R3 | | 7.8 | | |
| 4 | 8.9 | S at R5 | = | 9.7 | = | 1.09 |
| | | S at R4 | | 8.9 | | |
| 5 | 9.7 | S at R6 | = | 10.5 | = | 1.08 |
| | | S at R5 | | 9.7 | | |
| 6 | 10.5 | S at R7 | = | 11.2 | = | 1.07 |
| | | S at R6 | | 10.5 | | |
| 7 | 11.2 | S at R8 | = | 11.9 | = | 1.06 |
| | | S at R7 | | 11.2 | | |
| 8 | 11.9 | S at R9 | = | 12.5 | = | 1.05 |
| | , | S at R8 | | 11.9 | | |
| 9 | 12.5 | S at R10 | = | 13.0 | = | 1.03 |
| | | S at R9 | = | 12.5 | | |
| 10 | 13.0 | S at Tor | AL. | | | 10.16 |

Applying this value to the general formula for bondability, viz:-

$$B = \frac{9.00 - (T - 9.00)}{9.00} \times 100$$

Where B = % Bondability T = Total of Strength Divisions

Then,

$$B = 9.00 - \frac{(10.16 - 9.00) \times 100}{9.00}$$

$$= 87.1\%$$

The sand flowability may be regarded as a combination of uniformity of permeability and of uniformity of strength. It becomes therefore an average of the ventability and the bondability.

Thus, flowability = $\underbrace{V + B}_{a}$

or in the sand already considered

% Flowability =
$$\frac{82.3 + 87.1}{2}$$

= 84.7%

It may be necessary to consider the bondability or the ventability as a separate factor when discussing a specific sand condition, but wherever reference is made to flowability the figure quoted is obtained in the above described manner.

Summarizing the nomenclature and definitions used in the description of the tests mentioned, we have:—

Ventability—Uniformity of permeability over the mold surface in relationship to an ideal uniformity.

Bondability—Uniformity of strength over the mold surface in relationship to an ideal uniformity.

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VENTABILITY - BONDABILITY, PER CENT

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Flowability—Average of the ventability and bondability and having reference to the ability of a sand to form a mold surface of uniform properties, these properties being basically permeability and strength.

All the above properties are deduced mathematically by standard tests at 1, 2, 3, 4 and up to 10 rams of the Standard A.F.S. rammer.

This method of calculation described appears to be somewhat empirical but it does have a definite mathematical basis expressible by the following formulae. When for example we are considering the function ventability.

$$V = (R-1) - \left[\frac{P_1}{P_2} + \frac{P_2}{P_8} + \frac{P_3}{P_4} + \dots + \frac{PR-1}{PR} - (R-1) \right]$$

Where V = Ventability

P = permeability R = number of rams

This equation may be modified to read:-

$$V = 1 - \frac{\triangle P_{1-2}}{P_2} + \frac{\triangle P_{2-3}}{P_3} + \frac{\triangle PR - (R-1)}{PR}$$

Where $\triangle P_{1-2} - P_1 - P_2$ $\triangle P_{2-5} - P_2 - P_5$ etc.

This modified equation is an approximate expression of the following relation—

$$V = \frac{\int_{1}^{PR} \frac{-dP}{P}}{\int_{1}^{R} dR}$$

The difference of course between this and the previous modified equation is that one uses a finite number of ramming steps whereas the other is an integral with a mathematical infinite number of steps. This last equation may be integrated to yield:—

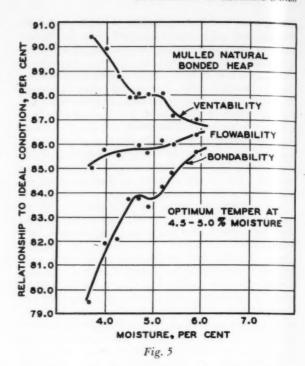
$$V = 1 - \frac{\ln \left(\frac{P_i}{P_B}\right)}{R-1}$$

It has been found that certain sands exhibit a definite straight line relationship when the properties and rams are plotted on log-log paper. This fact has been used in arriving at the ventability and bondability values but is not detailed here as it is preferred to calculate according to the methods already explained and arrive at a percentage value for *ventability* and *bondability*.

Flowability and Moisture Content

Results obtained on N.J. 125 sand by Dietert and Kyle have already been shown in Fig. 1, and it is quite evident that the methods of measurement used by these investigators do not tell the whole story.

To study the effect of moisture content on ventability, bondability and flowability, a batch of bench floor heap was mulled at different moisture contents, the variation in strength and permeability with ramming being measured at each moisture content. The



properties were calculated as already described and are shown in Fig. 5.

It should be realized that the clay content of this heap was high in that the mulling operation resulted in a strength of 10.8 lb at 4.5 per cent moisture and three rams.

The moisture range studied was confined to below the temper point and slightly above it, it being thought that an excessive moisture content would be of academic interest only, in that this particular sand is never worked above 6.0 per cent moisture. At the lowest moisture contents investigated the sand was actually too dry for practical working.

The first noticeable feature of the results is that the ventability curve is closely allied to that shown by Dietert in his measurement by the sand movement method. The bondability curve or strength-ram relationship closely follows that given by Kyle in his hardness gradient method. This is to be expected as mold hardness is closely related to strength.

The flowability curve is, of course, the average of the bondability and the ventability so that it may be said to give a value related to both the methods of measurement described, viz., Dietert's method and Kyle's method.

The general relationship between moisture and the properties of this sand is somewhat as follows:—

Ventability—Decrease in ventability with increasing moisture content with a definite leveling off at and around the optimum temper point.

Bondability—Increase in bondability with increasing moisture content with a leveling off at and around the optimum temper point.

Flowability—Gradual increase of flowability with moisture content with a leveling off in the optimum moisture range.

These results indicate that the properties tend to be

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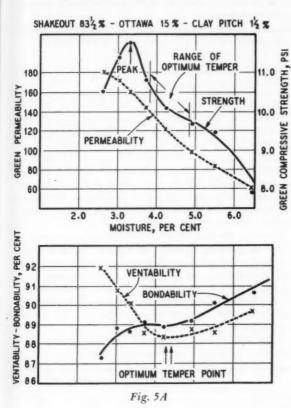
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more uniform in the optimum moisture range. This indicates the advisability of always working a sand at its correct temper point.

These results also lead to a good method of determining the optimum temper point of a sand and a typical example is given in Fig. 5A. In this case a synthetic sand was mulled at different moisture contents. The optimum temper point is at the valley shown in the ventability curve. Increase of moisture beyond the values given in the chart would result in a further rapid decrease in ventability.



Hardness Variations in a Test Mold

To trace the influence of flowability on the hardness of molds rammed in the foundry, a special pattern was prepared as shown in Fig. 6. This pattern is designed to emphasize the difficulty of obtaining a good hard mold surface on a vertical face.

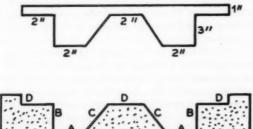


Fig. 6-Test pattern for ramming tests.

In conducting tests a drag half was rammed up using any given sand throughout the mold. Hardness measurements were conducted after withdrawing the pattern. The positions measured are marked in Fig. 6.

A = Pocket bottom

B = Vertical sides

C = Sloping sides

D = Mold top

All the values quoted represent averages of several readings taken in the position shown.

The results obtained with jolt ramming in a 13-in. x 16-in. flask, 5 in. deep are shown in Fig. 7. It should be noticed that jolting alone does not result in good mold hardness values.

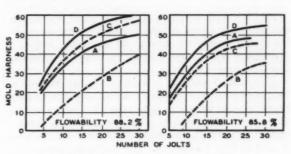


Fig. 7-Mold hardness and flowability.

The sands used were a mulled natural bonded heap and the same heap mulled with 25 per cent Michigan City silica sand. These had flowabilities of 85.8 and 88.2 per cent, respectively. The curves indicate that the sand with a higher flowability value gives higher mold hardnesses for the same degree of ramming or that to obtain a given mold hardness we require less ramming with the sand of higher flowability.

This is to be expected, but the tests indicate the difficulty of obtaining high hardness values on the vertical walls of a jolted mold even with a sand of good flowability. It is evident that the method of ramming is also important.

By jolt-squeezing several molds with sands of different flowability the following results were obtained:—

5 JOLTS AND SQUEEZE AT 90 LB PRESSURE HARDNESS READINGS ON MOLD FACE

| | | | Flowabilit | y Value | |
|---------------------------------|----------------------------------|------|------------|---------|------|
| | | 82.3 | 85.8 | 88.2 | 90.1 |
| Bottom | A | 74 | . 87 | 87 | 73 |
| Vertical Sides | В | 36 | 64 | 67 | 57 |
| Sloping Sides | C | 61 | 74 | 80 | 70 |
| Mold Top | D | 48 | 69 | 75 | 66 |
| | | - | 74 | - | - CH |
| Average A B C I |) | 60 | 74 | 77 | 67 |
| Difference between high and low | | | 23 | 20 | 16 |
| % Variation or H | $\frac{1gn - 1ow}{2} \times 100$ | 63 | 31 | 26 | 24 |
| A | verage | | | | |

The percentage variation of hardness over the mold has been calculated by dividing the highest hardness minus the lowest hardness by the average mold hardness. Expressing these results graphically, we have the curve shown in Fig. 8.

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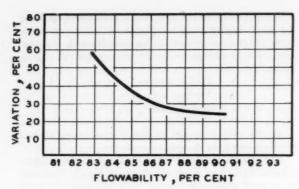


Fig. 8-Hardness variation vs flowability.

Figure 8 indicates that in the test mold under consideration the lowest hardness variation over the mold surface is somewhere around 20 per cent. Below a flowability of 85.0 per cent the variation in hardness increases quite rapidly.

The sand flowability is evidently quite important but, in order to have complete uniformity of properties over the mold surface, it is necessary to concentrate on the method of ramming. This can readily be demonstrated by ramming the mold to different degrees with a sand of high flowability. The results are set out below and the difference between the vertical walls and the mold bottom is taken as the standard of variance. In the results listed as "angle ram" the mold was jolted at an angle to make the vertical face nonvertical, reversed through 180 degrees, again jolted, and then finally squeezed.

HARDNESS VARIATIONS WITH SAND FLOWABILITY OF 90.1%

| Position of Readings | | Ramming Method | | | | | | | | |
|-------------------------|---|----------------|----------|-------------------------|---|----------|----------|--|--|--|
| | | 12 Jolts | 20 Jolts | 5 Jolts Squeeze 90 # | Angle Jolt. 10 Jolts Squeeze 90 # | Hand Ram | 30 Jolts | | | |
| Bottom | A | 39 | 42 | 73 | 77 | 52 | 71 | | | |
| Vertical Sides | В | 15 | 24 | 57 | 67 | 43 | 78 | | | |
| Sloping Sides | C | 39 | 43 | 70 | 72 | 50 | 65 | | | |
| Mold Top | D | 43 | 50 | 66 | 68 | 57 | 78 | | | |
| Average A B (| | 34 | 40 | 67 | 71 | 51 | 73 | | | |
| A and B | | 24 | 18 | 16 | 10 | 9 | 7 | | | |
| % Variation | | 70 | 45 | 24 | 14 | 18 | 10 | | | |

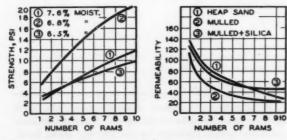


Fig. 9—Green strength and permeability vs number of rams in natural bonded sand.

It is evident that the best uniformity is obtained by the hand ram. In actual fact, perfect uniformity is possible, depending on the skill of the molder. In the hand ram this uniformity is not as dependent on flowability as it is in the case of pure mechanical ramming. Whatever the method of ramming studied, it would appear that hand peening on vertical faces of the mold would be highly advantageous.

Flowability Control

A few examples of flowability control are quoted below to indicate that the method of measurement has a practical value:—

| Bond | ability | Ventab | oility |
|------|---------|--------|--------|
| (1) | 81.4 | (1) | 78.3 |
| (2) | 83.4 | (2) | 79.3 |
| (3) | 85.1 | (3) 8 | 35.6 |

The curves in Fig. 9 were obtained on a natural bonded heap sand which influenced the facing sand made from this heap to the extent that castings showed burn-on in pockets and roughness on side walls.

Curve I represents the original heap condition. The flowability was 79.9 per cent and the strength of this heap was 4.7 lb. Mulling the heap to distribute the clay and to find out how much clay was available for bonding gave a strength of 10.3 lb and raised the flowability to 81.4 per cent.

Diluting the heap strength back to a more normal value by mulling in a 25 per cent addition of Port Crescent silica sand resulted in a strength of 5.0 lb and a flowability of 85.4 per cent. This represents an increase in flowability, of 5.5 per cent which entirely changed the feel of the facing sand made from this heap and eliminated the burnt-on and rough conditions in the casting.

This same procedure adopted to a lesser extent on a synthetic sand gave the following results:—

| | Strength | Ventability | Bondability | Flowability |
|-----------------|----------|-------------|-------------|-------------|
| Heap Sand | 4.4 | 84.2 | 86.7 | 85.5 |
| Mulled Heap | 9.5 | 84.4 | 87.1 | 85.8 |
| Mulled with 15% | | | | |
| Silica Sand | 7.3 | 87.3 | 89.4 | 88.4 |

Two illustrations may be given of changes in flowability accomplished in foundries using natural bonded sands. In the first case, the change was accomplished through the facing alone over a long period of time. The change aimed at was an improvement in ventability to allow molders to ram hard without running into scabs and blows. Hard ramming was necessary to give good finish on all castings ranging from ½ lb up to 200 lb in weight.

The change made in the facing consisted in cutting the new sand used (Taggart O) from 17 to 10 per cent and eliminating use of bentonite. At the same time, seacoal was replaced by oiless binder which decreased the fines due to combustible material in the sand. The operating range of strength was reduced over a period of time from 11 to 13 lb to 6 to 8 lb. A summary of the results is given below, and it should be stated that the casting finish and sand defects decreased as the ventability improved.

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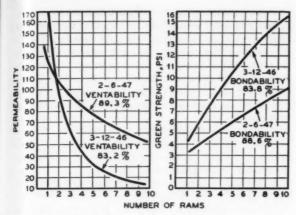


Fig. 10-Permeability and green strength vs number of rams.

| Date | Ventability | | |
|---------|-------------|--|--|
| 9/9/45 | 70.8% | | |
| 2/6/46 | 73.7% | | |
| 6/13/46 | 78.8% | | |
| 3/11/47 | 88.0% | | |

In the second case, the sand was converted by mulling the heap with a silica sand addition and altering the facing mix to further improve the flowability of the heap. Initial mulling of the heap was to adjust the grain size distribution.

Results taken before the change and about two months afterwards are shown in Fig. 10. This change in flowability resulted in a tremendous increase in casting finish and a cut down in the ramming time necessary to give good castings and freedom from cope drops.

3/12/46 Ventability 83.2 Bondability 83.8 Flowability 83.5 2/6/47 " 89.3 " 88.6 " 89.0

The procedure adopted in making this change

1. Mull the heaps with 25 per cent silica sand chosen to bring the grain size distribution to the desired value.

2. Prepare facing daily from the mulled heap using 25 per cent new sand in the proportions of 1 part silica sand to 1 part natural bonded sand.

It can be seen from the examples quoted that the flowability of any sand which is poor in flowability may readily be improved by paying attention to the elimination of two factors:—

1. Elimination of inert clay and silt by dilution or, if necessary by actual removal. Clay can only be diluted by using a silica sand and mulling in the clay already present in the sand to obtain distribution of this clay over the sand grains.

2. Aim at a distribution of grain which gives from 55 to 70 per cent of the grains on three adjacent screens. The actual change in distribution is determined by the condition of the sand to be changed and by the sands available for making the change. For this reason, no specific cases and grain size analysis have been included in this paper. Each case should be considered separately.

It may be said in general that the most frequent

cause of a poor flowability is the presence of a large proportion of clay, silt and other material below 200 mesh in size. Offsetting this condition by adding coarse grains to maintain permeability does not improve the flowability but often makes it worse.

Practical Flowability Values

Much has been said in this paper about the ideal sand from a flowability point of view, but it is often necessary to depart from the ideal for specific reasons. However, using this method of measurement and observing the casting finish and methods of ramming used, has enabled some practical working ranges to be established. These ranges must necessarily be considered in a broad sense, because the flowability capable of making a good casting depends so much on the method of ramming and the nature of the casting.

In general, it may be said that the highest ventability or uniformity of permeability with ramming that is obtainable is the value to work to. A high bondability, however, should be avoided as it is usually an indication of a brittle sand or a sand too low in clay content to keep away from buckling and other expansion difficulties. If, on the other hand, the bondability is low enough to affect the ventability adversely, trouble will be experienced with scabs, blows and burnt-on castings.

The following table indicates some flowabilities actually found in practice.

SOME ACTUAL FLOWABILITIES BEING USED

| Sand Type a | nd Mix | Ramming Method | | | d. Flow. oper Results |
|---------------|--------|-------------------|------|------|--------------------------|
| Heavy Green | 1 | | | | |
| Synthetic Mi | ix | | | | Results excel- |
| Port Crescer | it 85% | | | | lent. Buckling |
| Revivo | 31/2% | | | | prevented by |
| Dixie Bond | 31/2% | Jolt | | | Akro in mix. |
| Akro | 5% | Rollover | | | Otherwise bond |
| Seacoal | 2% | | | | ability would |
| Kordek | 1% | | 92.7 | 93.3 | 93.0 be too high. |
| Natural Bon | ded | | | | |
| Skin Dry Fa | cing | | | | |
| Heap Sand | 78% | | | | Inclined to scab. |
| Ottawa 75 | 15% | | | | Bond is too high |
| Revivo | 1% | Slinger | | | Poor ventability |
| Mogul | 1% | Backing | | | |
| Oilless Binde | er 5% | | 71.6 | 83.6 | 77.6 |
| Natural Bon | ded | | | | Good results. |
| Light Green | | | | | |
| Heap Sand | 78% | | | | |
| Bank Silica | 19% | | | | |
| Seacoal | 21/2% | Jolt Squeeze | | | |
| Green Bond | 1/2% | and Squeeze | 86.4 | 88.9 | 87.7 |
| Natural Bone | ded | | | | |
| Light Green | Facing | | | | |
| Неар | 84% | Squeeze and | | | |
| Millville 110 | 10% | jolt squeeze | | | Castings rough. |
| Seacoal | 5% | | | | |
| Bentonite | 1% | | 82.1 | 78.0 | 80.1 |
| Synthetic Lig | | | | | |
| Green Facing | | - | | | |
| Неар | 60% | Bench | | | |
| Juniata | 30% | Squeeze. | | | Excellent finish. |
| Fireclay | 31/2% | Jolt | | | Freedom from |
| Bentonite | 31/2% | Squeeze | | | sand defects. |
| No Vein | 1% | | | | *** |
| Permibond | 2% | | 92.1 | 89.4 | 90.8 |

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SOME ACTUAL FLOWABILITIES BEING USED (CONT.)

| | | Ramming | Vent | . Bond | l. Flo | w. |
|---------------|--------|--------------|------|--------|--------|------------------|
| Sand Type a | nd Mix | Method | A | t Tem | рет | Results |
| Synthetic H | eavy | | | | | |
| Green Facir | ng | | | | | |
| Hydroblast | 60% | Hand (Air | | | | |
| Port Cresce | nt 30% | Rammer) | | | | |
| Fireclay | 31/2% | | | | | Excellent result |
| Bentonite | 31/2% | | | | | |
| No Vein | 1% | | | | | |
| Permibond | 2% | | 91.7 | 91.2 | 91.5 | |
| Synthetic L | | | | | | |
| Green Facin | | | | | | |
| Heap Sand | 97% | Jolt | | | | Results moder- |
| Lawco Bond | 11/2% | Jolt Squeeze | 00.4 | 00.0 | 04.0 | ate. Rough |
| Permibond | 11/2% | Squeeze | 82.4 | 86.0 | 84.2 | Finish. |
| Natural Bon | | * 1. 0 | | | | |
| Med. Green | | | | | | 0 1 . 1 |
| Heap Sand | 88% | Squeeze | | | | Good tendency |
| Taggart O | | | | 09.0 | 0 . 0 | to rough finish |
| Oilless Bind | er 2% | Ram) | 87.8 | 83.8 | 85.8 | on sidewalls. |
| Natural Bon | ded | | | | | |
| Dry Sand Fa | | | | | | |
| Heap Sand | 49% | Air Rammer | | | | |
| Millville No. | 1 27% | | | | | Good. |
| Port Crescer | | | | | | |
| Permibond | 2% | | 82.1 | 87.4 | 84.8 | |
| Natural Bon | ded | | | | | |
| Dry Sand Fa | icing | | | | | |
| Coarse Silica | 30% | Air Rammer | | | | |
| Heap Sand | 42% | | | | | |
| Med. Silica | 8% | | | | | Poor. |
| Firesand | 8% | | | | | |
| Volclay | 6% | | | | | |
| Pitch | 6% | | 73.0 | 85.8 | 79. | 4 |
| Natural Bon | ded | | | | | |
| Light Green | | | | | | |
| No Facing | | Squeeze | | | | Moderate. Good |
| New Sand | | | | | | on thin flat |
| Albany | 11/2 | | 83.2 | 83.8 | 83.5 | work. |
| Natural Bon | ded | | | | | |
| Light Green | Facing | | | | | |
| Неар | 79% | | | | | |
| Taggart O | 12% | Squeeze | | | | Good |
| Bank Silica | 6% | | | | | |
| Seacoal | 3% | | 83.0 | 85.3 | 84.2 | |
| Natural Bon | ded | | | | | |
| Med. Green | | | | | | |
| No Facing | | | | | | Moderate |
| New Sand | | | | | | Roughness on |
| Albany | 21/2 | folt | 96.9 | 949 | OF C. | side walls. |

Summary and Conclusions

1. A method has been proposed and outlined for measurement of flowability of molding sands. It distinguishes two distinct properties, ventability and bondability. The average of these two is the flowability. The net result is a measurement which determines the latent ability of a sand to give uniform strength and permeability over the complete mold surface.

2. The relationship between mold properties and sand behavior in the laboratory must be followed by means of a hardness tester. Emphasis is placed on uniformity of hardness as related to flowability rather than actual high hardness. Hardness itself is dependent on the degree of ramming and the strength of

the mold.

3. A relationship has been established between flow-ability and mold uniformity on a typical mold with a given method or ramming. Absolute uniformity with this method of ramming, viz., jolt squeeze, can only be obtained by judicious hand peening, or possibly with special ramming equipment. In other words, the nature of the sand can do part of the work, the rest must be done by ramming to suit the casting being made.

4. Variation of flowability with moisture content indicates the need for always having the sand at its optimum temper point. Moisture control is essential

for uniform mold properties.

5. Typical examples of flowability control have been quoted and should be regarded as examples rather than directives. The exact procedure adopted to improve flowability will depend entirely on the circumstances existing. Generally, poor flowability is a sign of a high fines content and poor grain distribution often caused by this high fines content.

6. Synthetic sands usually have higher flowabilities than natural bonded sands, but by careful control flowabilities of around 90 per cent are possible with

natural bonded sands.

7. Typical flowability values found in practice have been tabulated. It may be said that the highest ventability possible is always the safest value to work to, but that the bondability should not go above 90 per cent unless a material like cereal is used to prevent buckling and expansion difficulties, and then only where the ramming is hard enough to insure adequate

strengths in the molds.

8. The control of flowability is definitely of practical benefit and is a means of avoiding most of the major defects associated with a sand condition. If a sand has good flowability, it usually means that the fines are reasonably low, the grain distribution is desirable and the permeability of the mold is high even with hard ramming. Reflection on these facts readily indicates how defects may be avoided by controlling flowability. In addition to this, it may be said that a good flowability is essential for a good surface finish on the castings although good flowability by itself will not guarantee a good finish.

DISCUSSION

Chairman: В. Н. Воотн, Carpenter Bros., Inc., Milwaukee. Co-Chairman: J. O. Ochsner, Crouse-Hinds Co., Syracuse, N. Y.

W. Davies (Written Discussion): 1 May I congratulate the author on his important contribution to the study of the shaping of molding sands. I should appreciate his comments on two

points.

The first concerns the moisture content of the sand mix. I am not sure that the proposed technique is adequate to contrast the characteristics of a kaolin-bonded mix with less than the optimum moisture for molding with those of a bentonite-bonded mix at optimum moisture; both mixes of course are based on the same high silica sand. In my experience an A.F.S. test piece of the former shows little increase in bulk density after the first blow of the rammer; in other words it would have a high percentage ventability as the permeability would change but slightly as ramming continued. Furthermore the strength of such a mix increases only slightly as ramming proceeds so that its bondability would be rated high. Overall the kaolin-bonded

¹ Research and Development Dept., The United Steel Companies Ltd., Moorgate, Rotherham, England.

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mix would have high flowability on the definition given by the author. The bentonite-bonded mix might well show greater changes in permeability and strength as ramming proceeds and so would be rated as having a lower flowability. In essence it would be a great help if the technique could be extended to take account of moisture content.

The second point is I think more serious. Could the author extend his technique to take account of the bulk density of the mix before ramming commences? In the ramming of a mold much of the redistribution of the sand takes place before the sand has developed appreciable green strength, that is, at bulk densities well below those obtained by one blow of the A.F.S. rammer. One of the features of a bentonite-bonded sand after it has been milled and aerated is at its low bulk density, often as low as 0.65 gm per cc on normal mixes. Even under optimum conditions a kaolin-bonded sand rarely attains a bulk density below 0.85 gm per cc. As a practical point this fluffiness of the bentonite-bonded sand is greatly valued by the molder as it enables him to ram more uniformly. This aspect is dealt with more fully in my book on Foundry Sand Control.

Finally may I say that I find myself in complete agreement with the author's theory of clay as a binding material.

MR. MOORE (Reply to Mr. Davies): Before treating the two points Mr. Davies has raised, specifically, I would like to say that I consider the use of the word "flowability" was quite unfortunate. In actual fact "flowability" brings to mind a property which is akin to viscosity whereas the thoughts outlined in my paper were only considered with the ability of a given sand mix to give uniform properties in the final mold. I believe this is the final criterion of sand behavior and quite logically, a sand which shows little change in permeability or in strength over the ramming range chosen is most likely to give a mold of uniform properties. This is only true, however, where the ramming degree employed in the test is comparable to the ramming degree used in practice.

In answer to the first point, the effect of moisture is of major importance. The various curves in the paper show quite clearly the degree of variance of both ventability and bondability with moisture content. However, these curves particularly Fig. 5 show a definite range over which these properties tend to level out or where they are less affected by the degree of temper.

In using these tests under practical foundry conditions, I always try to measure the ventability and bondability at a moisture content which falls within this range. By doing this a more fruitful comparison of variation in the ram properties may be made. In many cases, I have allowed a sample of sand to dry out or have added extra moisture in order to be able to sample the flowability at a definite fixed moisture content.

All this is quite commendable from the measuring point of view, but we should nevertheless realize that these properties vary with the moisture content and that the moisture content varies in any foundry under shop conditions. Perhaps then we should measure the properties at the moisture level we actually find in the sand.

With regards to the second point, Mr. Davies is absolutely right. The test was developed using standard equipment available in any foundry. In actual fact, however, I felt at the time that a ram of considerably less than one ram would be needed on some sands. Since that time I have used a rammer of the piston type activated by hydraulic pressure which has enabled me to extend this ramming range from an extremely light to an extremely heavy ram. With this rammer any sand may be tested under a ramming degree suited to the ramming actually found under shop conditions whether it be hand ramming, jolting, squeezing or slinger ramming. When adopting this procedure it is possible to more closely differentiate between sands which apparently have the same ventability and bondability as measured on the A.F.S. rammer.

The procedure also will allow a differentiation to be made between clays of different characteristics. In this connection I have also found that the difference between ventability and bondability on a sand offers some indication of the type of clay. For example, a silty type of clay which is not very colloidal in nature will also tend to give a higher percentage ventability than bondability at the optimum temper point whereas with a colloidal clay this relationship is normally reversed.

BRUCE BOECKER: 2 I take it that you like a small screen spread in your sand. Is that correct?

Mr. Moore: If we desire only a uniform permeability in the mold, regardless of how we ram the mold, we must have a uniform grain; it must not vary over a wide number of screens. That is the whole basis on which this test was developed.

However, this condition does not necessarily guarantee a good casting because it is a condition which will lead to high expansion in a mold. Unless we have something in our sand to offset these expansion forces, we will have defects, but nevertheless we will have a more uniform mold if we have a more uniform spread of sand grain size.

Mr. BOECKER: Would you suggest spread screen sand, a

medium sand, or a very uniform sand?

Mr. Moore: That depends entirely on the type of casting you are making and how you are ramming the mold. If you are making flat plates or similar type of casting, and ramming the mold fairly hard, do not get your grain size too uniform, because buckles will occur. You must have a certain spread of grain, a certain ability for the sand to move when it is heated, so high expansion forces do not develop.

If, on the other hand, you want an extremely fine finish and you are forced to use a very fine grain sand, you must keep that sand uniform in order to make high moldability and to avoid

scabs and similar defects.

J. B. CAINE: 3 I strenuously object to the term "flowability" as it is used in this paper. There is just one practical reason for my stand. We now have five definitions for flowability and Mr. Moore has given us a sixth. As Chairman of the A.F.S. Flowability Committee I find this is of personal importance and should be important to all working with foundry sand.

We must guard against loose terminology, because if I am talking in terms of Dietert's flowability, somebody else may be talking in terms of Moore's flowability, and a third person may be taking in terms of Kyle's flowability, the result is utter confusion. I earnestly beg some other term be used, rather than flowability; call it Flowability No. 1 or No. 5, so that we eliminate this confusion in terminology that we have today.

It is my personal opinion that the term flowability should describe a more basic property of sand than the one that is determined on the basis of permeability and strength. This is so simply because permeability and strength, in my estimation, can vary by many independent factors that flowability should be free of.

I am skeptical of predicting casting performance generally on the basis of room temperature properties. Another suggestion I would like to make is that the results be specified for iron molding sands only, because a number of the results do not apply to steel or, if they do apply there is another factor entering. I think the examples given in the paper should be specifically tied down to specific iron foundries and iron practices.

MR. MOORE: I thoroughly agree as to the terminology. However, we had to call it something, and flowability is what we called this property. We do not like it. However, I want to point out again the basis for the whole test. We recognize there are two functions to this property of flowability. One is natural viscosity, much like an oil as against, let us say, water. We have to overcome frictional force in order to pack our sand That takes a certain amount of energy. The other one is the uniformity of mold conditions that we get, and when we do, we overcome these forces. It is that latter one we are concerned with in this particular test. We have chosen permeability and strength for a definite reason. We say the foundryman, provided he can cause his sand to flow, does not care what the flowability is. He does want a uniform mold. A uniform permeability is a sign of uniform void condition in the mold. We need it to carry gases and keep our gas pressure forces low. We need strength to offset these expansion forces and gas pressure forces, and to offset the ferrostatic pressure of the metal, both at room temperature and high temperature.

I will agree that room temperature tests are not always indicative of what happens in the mold at higher temperature, but we find in general, if we start off with a strong stand at room temperature, that sand maintains its strength over a certain heating period while our casting is being poured, so there is some con-

nection.

Again, in this test we have never mentioned the actual permeability value or the actual strength value which you need

Sand Technician, Minster Machine Co., Minster, Ohio. ⁸ Metallurgical Consultant, Wyoming, Ohio.

in your mold. We have stressed purely uniformity of properties. Naturally these properties must be at a correct level for the particular casting being made. I do not care whether it is brass, iron or steel. We must have a certain density, a certain permeability, a certain strength in our mold to meet the conditions, and we can only assure uniformity from mold to mold if we have a sand which does not change in these properties appreciably as we change our ramming, which we cannot help in the shop.

MR. CAINE: I agree with Mr. Moore on this, but when you say, as you do in the Table in the paper, "Results excellent, buckling prevented, you are evaluating sand. That is my objection. If you say "controlling the sand," then I say, yes, we are agreed. It is the evaluation that is implied, that I object to.

L. B. OSBORN: 4 In evaluating sands, I would like to add some comments to Mr. Caine's statements. I do not think we should be too dogmatic in assuming that a sand of, say, essentially a three screen distribution would necessarily have a high expansion, because there are a few exceptions. There may not be many exceptions in the case of synthetic sand, but in natural sand there are a few exceptions where you find a very concentrated screen distribution together with low expansion; and when it is possible to select such a sand you have relative freedom from rat-tailing, buckling and scabbing and also have flowability and permeability at the same time.

Of course, the natural combination of clay and sand has much to do with good sand performance; but even when you consider washed sands of almost identical screen distribution we have seen as much as 40 per cent difference in the quartz tube confined expansion at 1500 F. Thus, if we are going to lay down a rule that we must always beware of a concentrated screen distribution, it is going to be misleading and may cause some foundrymen to arbitrarily reject a perfectly remarkable sand

just because of that one feature.

Therefore, I think it is important that we consider the high temperature tests, rather than depending too much on room temperature tests which are indeed excellent for control purposes, as Mr. Caine has stated; but before we approve or condemn the sand just on one or two features, it is important that we make actual foundry tests to see exactly what happens when the metal hits the sand.

Mr. Moore: I believe there is some misunderstanding. When I talk about grain distribution I speak of particle size and I include in that distribution in my own mind, both the clay and

the silt, as well as the grain.

You say that a natural-bonded sand on three sieves may not necessarily be bad. I say it is not on three sieves, as we have much silt and other fine materials which are of definite advantage. We can still have a high ventability or bondability with

this type of sand, providing that our bond is not of the gummy nature and does not prevent our grains from flowing together.

JULES HENRY: 5 Mr. Moore is to be congratulated on his presentation of the new idea of flowability. In the past, although I have had no experience with Kyle's flowability test, Dietert's flowability test has not been nearly as sensitive and reproducible as other sand tests. I am hoping that in the future we might be able to get a more reproducible flowability test using Mr. Moore's method. Has the unreliability of Dietert's and Kyle's flowability tests been one of the reasons for developing this new idea of flowability?

Mr. Moore: Yes, because in Fig. 1, one curve shows decreasing flowability and another shows increasing flowability over the same range of moisture content. Which can you believe?

However, that was not what we used to arrive at this test, When we had arrived at this test on theoretical consideration, we tested it under the same conditions and we found it was actually an average of Dietert's and Kyle's tests, so we thought that is very nice for everybody.

There is one further point I will make in regard to this test.

Since this original method of calculation we found that many sands, in fact most of the sands, exhibit, if we plot the results on a log-log paper or semi-log paper, a straight line relationship and because of this instead of having to go from 1, 2, 3, 4, all the way to 10 rams and make an involved calculation, we can take one ram or ten rams and arrive at our flowability from those two tests. We hope to publish that data later on. It makes the test simpler, but you must realize that it applies only to a sand which exhibits this straight line relationship, usually a high

E. L. KOTZIN: 6 Has the author had experience in the use of wetting agents to increase the flowability?

MR. MOORE: My experience with wetting agents is limited. However, I have added wetting agents to sands and the particular ones I used actually decreased the bonding efficiency of the clay, and in so doing they automatically increased the flowability of the sand.

Mr. Boecker: Why do you put the optimum moisture at the lowest point of the first drop (Fig. 5). The permeability curve dropped, then it went up and dropped again. Could you not take the optimum moisture, since the moisture would be higher at that place, where the curve reached its peak after the first drop, rather than at the lowest point before it went up again? That would give you a higher moisture and probably make your sand more workable.

MR. MOORE: Over any range of moisture shown in Fig. 5 the sand is definitely workable, but we asked ourselves the question, "What is the best point? What is the best point in that whole range for the sand?" and I thought that was it. I had no reason for this thought. I then conducted toughness measurements on the sand and found that point corresponded to the point of maximum toughness in the sand and could therefore be regarded as the optimum temper point. Those results were not reproduced in this paper.

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⁴ Sales Manager, Tri-State Sand Co., Corinth, Miss.

⁶ Metallurgist, Forest City Foundries Co., Cleveland.

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PRINCIPLES OF GATING DESIGN

FACTORS INFLUENCING MOLTEN STEEL FLOW FROM FINGER GATING SYSTEMS

By

W. H. Johnson, W. O. Baker and W. S. Pellini*

This investigation is part of a program to evaluate the fluid flow characteristics of molten metals in sand molds in order to evolve principles generally applicable to the design of gating systems. A previous report¹ showed that many gating systems do not function as commonly supposed. The present report entails a study of factors which influence the uniformity of discharge of molten steel into a mold cavity through multiple openings in a horizontal plane, as in finger gating systems, or which determine the sequence of flow through openings arranged in a vertical plane, as in step gating systems.

These studies are based on observations of the discharge of the molten steel into the mold cavities as recorded by motion photography. The most instructive scenes have been assembled into two 16 mm colored sound films, "Finger Gating" and "Step Gating."

Experimental Procedures

Molten steel was poured at 3000 F into both finger gating and step gating systems. The flow and appearance of the metal as it emerged from the gates were recorded by 16 mm kodachrome cinephotography at 64 frames per second. The luminescence of molten steel permits photographing the flow behavior from directly above the mold without artificial lighting.

Studies were made in this manner of flow in finger gating systems having a finger to sprue area ratio of two to one. The flow was observed in runners designed

to split the stream; in runners bent toward or away from the casting; in tapered runners; in runners designed so that the molten metal impinged upon itself; and in runners having enlarged sections or pools.

Metal flow was observed in step gate systems having each step separately joined with the sprue; systems with two or more steps in common junction with the sprue; systems which incorporated tapered sprues or reversed sprues; and systems having steps combined with pools and offsets. The photographic technique employed in this study did not permit continued observation of the feeding from a step after the rising metal had passed that step; consequently, it was possible to determine only if the steps fed in sequence when reached by the metal.

Unless noted otherwise in the text the dimensions of the components of the gating systems studied were: (1) sprues and steps, 1 in. in diameter; (2) runner, 1 in. wide by 1 in. deep and rounded on the bottom by a radius equal to one half of the width; and (3) fingers, same shape as the runner with the appropriate dimensions.

In the step gating systems the steps were located 6 in. apart vertically. The casting used in the finger gate studies was a 6x12x6-in. section cast horizontally, while in the step gating studies, a casting 18 in. high, tapering from 6x6 in. at the top to 4x4 in. at the bottom, was cast vertically. A plugged pouring cup was employed throughout, since previous work with high speed photography (1000 frames per second) showed that such a cup maintained more constant conditions.

The following sections are devoted to generalized

* U. S. Naval Research Laboratory, Office of Naval Research, Washington, D. C.



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Fig. 1



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observations and analyses of now through the various gating systems.

Flow in Finger Gating Systems

Split Stream Runners: The gating system shown in Fig. 1 was so designed that the runner-finger intersections would act to split the runner stream into equal parts and thus force uniform feeding. In this system all the fingers fed practically from the beginning of pouring as shown in Fig. 1A. The flow through the finger was intermittent rather than steady. A rocking movement back and forth on the surface of the steel with absence of either swirling or rolling motion was observed,* probably due to the intermittent feeding.

For better observation of the flow in the fingers, the cope of the mold was removed, Fig. 1B. The intermittent nature of the flow is evident in the middle fingers. The absence of the cope affected the flow in that a greater quantity of metal entered through the outer fingers than was the case with the cope present.

Bent Runners: The four gating systems of Fig. 2 were designed to determine the effect on flow of changes in angle at the junction of the runner and fingers. With both ends of the runner bent back 45° from the casting, most of the feeding during the very early stages was through the outer fingers, Fig. 2A, producing swirling turbulence. A change to uniform flow conditions soon occurred as indicated by the development of rolling turbulence.

With the runner bent back only 30°, it was again observed that the initial feeding occurred through the outer fingers, Fig. 2B, resulting in swirling turbulence, while the outer fingers continued to feed most of the metal, as indicated by the persistence of swirling motion. Runners bent forward at angles of either 45° or 30°, Figs. 2C and 2D, caused the outer fingers to do almost the entire feeding in the early stages of pouring. Uniform flow was obtained late in the pouring when the runner was bent at an angle of 45°, but

* For purposes of this report, mold turbulence has been dif-

(1) Swirling-Caused by non-uniform flow; the axis of rotation being perpendicular to the surface of the metal and to the plane

(2) Rolling-Produced by uniform flow and characterized by

a uniform flow on the surface of the metal from one side to the

opposite; the axis of rotation of metal within the mold being horizontal and parallel to the plane of the finger and to the

swirling turbulence prevailed throughout when the angle of the runner was only 30°.

The system shown in Fig. 3 was designed to evaluate bent runners in connection with offset sprues. The runner in this case was placed at an angle of 45° to the casting, and it is seen that initially the farthermost fingers fed more strongly. Later, feeding became uniform through all fingers, as indicated by the rolling motion of the metal in the casting.

Tapered Runners: The gating systems of Fig. 4 were designed to study the effects of tapered runners. The width and depth dimensions (square cross section) of the first system tapered from 1 in. to 3/4 in. The sprue was located on the larger end. Initially, flow was stronger through the outer fingers, Fig. 4A, but uniform flow occurred later as indicated by rolling turbulence.

A taper modification to a 1/4 in. (width and depth) at the small end produced a more uniform first flow, Fig. 4B, but not sufficiently so to completely prevent initial swirling turbulence. Uniform flow was developed later as manifested by rolling turbulence.

Figure 4C shows the initial flow in a gating system in which the runner was step-tapered so as to reduce



Fig. 2



(3) Rocking-Produced by intermittent discharge and characterized by rocking back and forth of the metal in the mold.



ferentiated into three types:

of the fingers.

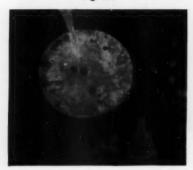
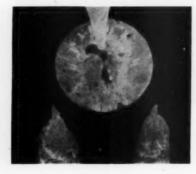


Fig. 28





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the volume abruptly at each finger junction. The overall taper varied from 1 in. at the sprue to 3% in. at the end. The nearer fingers fed most strongly at first; later the farther ones carried the greater quantity of metal, and finally, as the mold cavity filled, the feeding became uniform causing the turbulence to change from swirling to rolling.

Impinging Runners: These runners were designed to effect a reduction of momentum by the collision of two metal streams. The arrangement shown on the left in Fig. 5 resulted in greater flow through the inner fingers, Fig. 5A. Uneven flow occurred during most of the pouring, changing to uniform flow only at the final stages of pouring.

Figure 5B shows the initial flow from an impinging runner design having an enlargement at the point of impingement. This enlarged section was added so



Fig. 3—Designed to evaluate bent runners in connection with offset sprues, the runner in this system is placed at an angle of 45 degrees to the casting.

that the pressure wave generated at the point of collision could be dissipated. In this system, the inner fingers did most of the initial feeding, but uniform flow was established soon thereafter.

Enlarged Runners: Figure 6 shows gating systems designed to permit the metal to reduce its momentum in an enlarged runner; while Fig. 6A shows the effect of a single large pool. This system produced slightly uneven flow at first (emphasis is on farther fingers), but by the time the bottom of the mold was covered, all fingers fed uniformly and rolling turbulence resulted from the uniform flow.

Figure 6B shows a system with a runner enlargement in the drag in order to form two separate, vertical pools. This system produced a slower and uniform flow almost from the start of pouring which prevailed as the mold was filled.

Figure 6C shows a runner with two enlarged sections in the horizontal plane. With this arrangement flow was principally through the outer fingers as indicated by the swirling turbulence which continued throughout pouring. Reversing the pool as in Fig. 6D, in order to turn the flow away from the casting, resulted in some unequal feeding at first which later changed to uniform flow. With the cope removed, it was observed that the metal first swirled in the pools, lost part of its

momentum and then flowed out fairly uniformly into the casting cavity.

Summary of Flow in Finger Gates: Figures 1 through 6 show that it is possible to obtain uniform flow in "non-choking" finger gate systems, i.e., systems having a total finger area greater than the sprue area. This can be done by the use of gates which either split the fluid stream into equal parts or which absorb the momentum of the stream. The absorption of momentum can be best obtained by the use of pools in the runner and with lesser success by either bending the runner away from the casting, or tapering the runner.

Flow in Step Gating Systems

Inasmuch as single photographs are not descriptive of the flow in step gating systems because of the continuously rising metal, a recourse was made to schematic representation of the chronological details shown by the film. These are presented as Figs. 7 to 11. The position of the arrowhead on arrows emanating from the upper steps indicate the level of metal in the mold when flow started from the step in question.

The inclination of the arrow from the bottom step indicates the path of the first flow from that step. The flow desired in step gating, termed sequence flow, is a relatively strong but quiet flow from each step as the metal level reaches it. Dribbling is not desirable because of the possibilities of sand erosion and the formation of cold shuts.

Single Junction Steps: The first system, Fig. 7A, was used as reference since it represents the most commonly used system. With this system all of the feeding was accomplished by the bottom step. Metal ran back into the middle and top steps. The remainder of the systems in this series are modifications of this basic system based on reversing or steeply inclining one or more of the steps.

In order to reduce the predominance of the feeding by the bottom step, this step was inclined upwards 60° from the horizontal, Fig. 7B, thus simultaneously increasing the length of the step and introducing a sharp angle which offers more resistance to flow. This change produced sequence flow from the middle step but not the third. Inclining both of the two lower steps, Fig. 7C, did not produce sequence flow and moreover caused premature feeding from the middle step.

Design for Sequence Flow

Figures 7D, 7E, 7F show systems aimed at producing sequence flow with steps which curve downward from the sprue, and with curved steps in combination with inclined steps.

The first gating system, Fig 7D, illustrates a continuous step gate which is sometimes used commercially. It is claimed that in such a gate the first metal will follow the contour of the gate and subsequent incoming metal will be deflected by the metal in the gate so that feeding occurs at continuously higher levels during pouring. However, this gate acted to deflect the metal to the bottom of the mold cavity throughout pouring. The second system, Fig 7E, divided the continuous gate into three steps but was not successful insofar as the top step was concerned.

Combining steps that curve downward from the sprue with reversed angles, as in Fig. 7F, produced lim-

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FINGER GATES
TAPERED RUNNERS

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Fig. 4



. 4A

ited sequence flow from all three steps. However, dribbling occurred from the curved steps prior to flow. In order to eliminate dribbling, the middle step was changed from a downward curve to a straight gate inclined upward 30°, Fig. 7G.

With this system the bottom step discharged metal forcibly when pouring started; the middle step did not dribble, but fed when the metal level reached it; the top step, however, still dribbled, but fed in sequence when the metal level approached it. Thus limited sequence flow can be obtained from a three-step system when the top step is curved downward and the lower steps are inclined upward.

Tapered Sprues: The next series of gating systems, Fig. 8, shows the effect of tapering the sprue. The straight step gating system, Fig. 8A, is used as reference. Flow through gates attached to sprues which had a direct taper (from 1 in. at the top to 3/4 in., 1/2 in. and 1/4 in. at the bottom, Figs. 8B to 8D, respectively) was sequential to a limited extent only for 1-in. to 3/4-in. taper. The more severe tapers resulted in almost simultaneous flow through the three gates.

Sprue Taper Critical

The first reverse taper system, Fig. 8E, was a bigend-down sprue tapered from a 1-in. diameter at the top to 4 in. at the bottom. This system was not successful. A milder reverse taper system, Fig. 8F, had the sprue tapered from 1-in. diameter at the top to only 2 in. with a pool at the bottom. Again the system was not successful.

These experiments corroborated the deductions relative to the finger gates; namely, the amount of sprue taper necessary to produce the desired flow pattern is critical and would be difficult to control. Reverse tapers (which may also be considered enlargements) show, as in finger gates, that the use of enlargements decreases the momentum of the incoming metal but not sufficiently to produce the desired sequence flow in all cases.

Common Junction Steps: Figure 9 shows the use of a novel system of slanting two or more of the steps upward from a common junction at the sprue. Figures 9A, 9B and 9C show gate systems with all three steps radiating from a common junction. In the first two, limited sequence flow was obtained through only the lower two steps. In the third, Fig. 9C, for which the angle of inclination of the lower step was 60°, metal ran back into the top step at first, but later an indication of weak flow from this step was apparent.

In attempts to improve the flow from the top step, the second group of gating systems, Figs. 9D through

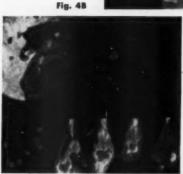


Fig. 4

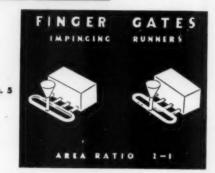
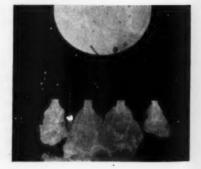




Fig. 58

Fig. 5



9F, had only the two lower steps joined at a common junction. The top step was divorced because it was believed that the flow distance in the top step from the multiple junction to the point of entry at the casing was too great to permit the desired flow. With the top step set horizontally, Fig. 9D, feeding was slightly better through this step than in the systems of 9A to 9C, but it was still very weak.

Inclining the top step 30°, Fig. 9E, did not produce better results. A beneficial effect on the flow, however, was produced by inclining the top step 60°, as in Fig. 9F. The lower steps fed in order, and although at first metal ran back into the top step, strong surface turbulence later indicated that this step was func-

tioning properly.

Figures 9G through 9I show a change in the location of the junction point so that only the upper two steps are connected. The bottom step was retained steeply inclined upward at 60° for comparison with Fig. 9C. In the first system, Fig. 9G, with the middle step parallel to the bottom step, strong initial flow occurred at the bottom step and sequential flow was obtained from the middle step, but metal ran back into the top step.

Common Junction Step Angles

Changing the angle of the middle step to 45° and to 30°, Figs. 9H and 9I, respectively, progressively improved the flow from the top steps. Although metal ran into the top steps at first, flow soon occurred from these in sequence; flow from the bottom steps was still strong. In order to decrease the jet effect of the flow from the bottom step the step was inclined only 45°, Fig. 9J. Sequence flow with reduced force from the bottom step resulted.

The systems in Fig. 9 show that (1) with proper design the desired sequence flow occurred from all three steps; (2) that the bottom step must be inclined so that after the first flow occurs preference for the con-

tinuance of flow through this step is decreased; and (3) that the most satisfactory sequence flow from the upper two steps is obtained when these steps radiate from a common junction.

Reversed Sprues: The functioning of step gating systems with reversed sprues is shown in Fig. 10 using the common "saxophone" or "trombone" type of gate. In the first system flow was obtained from all three steps, but dribbling and premature flow occurred from the middle step. In order to reduce the momentum of the metal stream, which was believed the basic cause of such premature feeding, enlarged sections, Figs. 10B and 10C, replaced the bend in the sprue.

In the system of Fig. 10B the bottom step fed quietly and the top step correctly, but the middle step fed early. To overcome premature feeding from this step, the reversed sprue portion was enlarged to a 2-in. diameter at the base and tapered to a 1-in. diameter at the top, Fig. 10C. The bottom step again fed very quietly. The middle step began to feed at a later time than before, and the top step fed in sequence.

These systems show that sequence flow in step gates as in the case of finger gates can be obtained most readily by devices which dissipate the momentum of the incoming metal without producing localized pres-

sure zones at the ingate junctions.

Pools and Offsets: Various systems were studied in which pools or offsets were used, Fig. 11. It was thought that momentum effects could be eliminated by such means and sequential feeding obtained. However, pools caused strong premature feeding from the top step and flow was only occasionally observed from the lower steps. These tests indicated that the use of pools or offsets does not, per se, produce sequence flow.

Summary of Flow in Step Gates: The step gating systems in Figs. 7 through 11 show that sequence flow can generally be obtained by designs that absorb the momentum of the fluid stream or reverse the direction

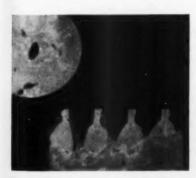




Fig. 6/



Fig. 6

Fig. 6C





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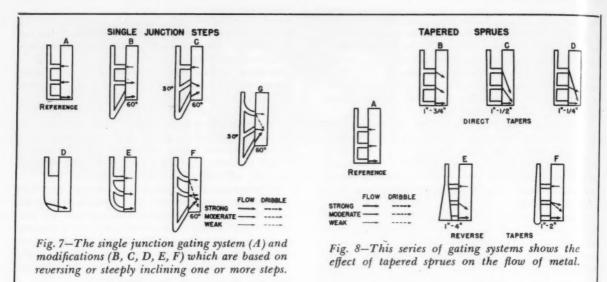
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of flow. Of the systems studied, sequence flow was obtained most readily by the use of common junction step gates, Fig. 9, and reversed sprue step gates, Fig. 10.

Generalized Hydrodynamics of Liquid Metal Flow: The gating of metal castings presents a complicated problem of fluid dynamics. In part, the flow of molten metal is that of a freely falling liquid under the influence of gravity. In part, it approximates the steady state flow of a fluid in a pressurized system. Both types of flow probably are always present in any fluid system, but to a different degree depending on various factors. Frequently turbulence may also be present.

It appears from the observations reported here that certain established concepts of fluid mechanics could be used to advantage by foundrymen in designing gating systems. Gating systems in present use indicate that the importance of momentum effects is not appreciated. A realization of the importance of these effects for fluids of high density, such as molten metals, is imperative for proper design.

From a strictly mechanical viewpoint, casting into a mold involves simply the transfer of metal from a position of high gravitational energy to one of lower energy. Inasmuch as energies may only be changed in form but not lost, it follows that the decrease of gravitational energy of high position must occur by transfer to other forms of energy. The relationships involved are strictly defined by the Bernouilli theorem, which is one of the basic and classic laws of hydrodynamics. This law states that any fluid has an instantaneous energy which is composed of three elements as follows:

 $Et = Mhg + \frac{1}{2}Mv^2 + PV$

= energy of position + energy of momentum + energy of compressive elastic action

where E = energy; h = height of fall; g = acceleration due to gravity; M = mass; P = pressure; v = velocity; V = volume

Analysis on a basis that the flow of molten metals may be compared to the flow of water in the same channels is dangerous, even as a first approximation, for cases involving rapid flow. In these cases, such as shown for the initial entry of the metal along the empty channels, the velocity parameter of the ½Mv² term (momentum) becomes the controlling factor of the equation since the pressure differential in incompletely filled channels is very low, and the PV term is therefore essentially zero.

Because of the importance of the momentum during the initial stages of pouring, it is reasonable to expect that the eightfold difference in the densities of water and liquid steel should result in a greater dependence of steel flow on momentum effects. For conditions involving slow flow in completely filled channels, described as pressurized systems, it should be expected that momentum effects would be of less importance. Accordingly, fluids of similar kinematic viscosity but with different densities should behave similarly in slow flow.

Sprue Phase of Pouring—Development of Momentum: A unit mass of metal flowing from the sprue to the mold through a distance (h) will lose an amount of potential energy (hg) and acquire a corresponding amount of kinetic energy Mv^2 (where $v = \sqrt{2hg}$), except for frictional losses, which represent nonreversible losses of energy by the system to the mold proper.

It should be understood that the total energy of the fluid system is finally converted to heat and transferred as such to the mold. The total height (h) is not measured from either the pouring hole in the sprue cup or the point of entry into the mold, but is measured from fluid level to fluid level. The important fact is that (h) decreases during the pouring operation while the liquid levels change as the mold fills and the sprue cup empties.

Once the plug is removed from the sprue cup, the metal begins to fall freely with accelerated motion. It should then fall in a stream of decreasing cross section in keeping with the well-known hydraulic relation:

Av = Q (constant) where (A) is the cross-sectional area. This expression follows simply from the incompressibility of the fluid and the constancy of volume. Since $v^2 = 2hg$, (A)

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should change according to the relation derived below: $A = Q/v = Q/\sqrt{2hg}$

The velocity increase developed during the course of flowing down the sprue should theoretically result in a decreased pressure leading to aspiration of air. The importance of such aspiration in metal casting is not clear. True aspirator action can exist only when the metal stream completely fills the top of the sprue. When the metal is falling freely, but not completely filling the top of the sprue, even though (v) is increasing, no true aspirator action is to be expected because the pressure (p) is necessarily equal to atmospheric.

However, it is extremely unlikely that the falling stream falls free and unimpeded. In all probability, it strikes and restrikes the sprue walls to produce tur-

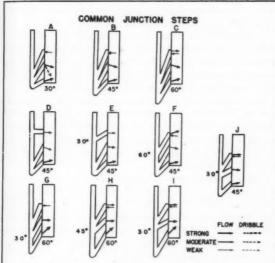


Fig. 9—In this system two or more steps slant upward from a common junction at the sprue.

bulent flow. Thus it is possible that frequently what has been considered free fall of the metal is really a combination of free fall and turbulent flow. Under these conditions in vertical systems, considerable gas may be mechanically trapped, carried into the mold, and replaced by additional gas or air drawn into the gate by a pseudo-aspirator or entrainment effect.

Runner Phase of Pouring—Development of Pressure Zones: The metal after falling through the sprue and entering the runner takes the most rectilinear path available and travels with increasing velocity (gravitational energy converted to kinetic energy) along the empty runner until the end of the runner is met. This is particularly evident in finger gating systems. A pressure wave is then generated at the point of impingement (kinetic energy is changed to energy of elastic compression) causing the farthermost ingates to feed prematurely and for ibly. The pressure then builds up along the entire system and, depending upon the geometry of the system, other fingers begin to feed.

The flow patterns observed from bent runner systems in finger gates indicate that the geometry of the system is critical in determining the effects of the pressure zone built up at the end of the runner. With the runner bent back from the casting at 45°, the sharp change in direction at the junction of the fingers and runners causes increased flow resistance through the outer fingers, thereby producing uniform flow through all fingers.

When the runner is bent back only at a 30° angle, the change in angle of flow from runner to fingers is not sufficient to reduce appreciably the preferential flow in the outer fingers. The flow behavior observed when the runner is bent toward the casting is the result, at least in part, of splitting of the stream at the finger-runner junctions. In the case of the runner bent forward at an angle of 45°, this tendency is pronounced.

The use of enlarged sections or pools appears to be a most promising means of obtaining uniform or sequence flow. These devices permit the molten metal to lose a great proportion of the initial momentum without the generation of pressure zones and thus aid in producing uniform flow. Runner systems with impinging streams are apparently not as effective as others in producing uniform flow through finger gates because pressure wave effects are not eliminated.

Tapering may be used to control flow by frictional effects but its action appears to be more critical than the other methods explored in these tests.

Ingate and Sprue Areas

In most finger gating systems multiple ingates feed into the mold from a single downgate. It has been shown here that, in non-choking finger gating systems having the total area of the ingates greater than that of the sprue, the ingates do not normally feed uniformly and require special geometrical features to do so. Whenever the total area of the ingates is less than that of the sprue, the metal should be expected to back (choke) in the gating system and produce uniform feeding. Such conditions approximate those of theoretical steady flow in pressurized systems. Investigations of such systems are in progress.

Past designs of step gates apparently have been based on the anticipation of slow, even rise of metal and as such have ignored momentum effects. When the pouring conditions are predominately those of slow, steady flow, step gates might be expected to function most nearly as hoped. Under these conditions it would be expected that the bottom step would feed alone until the liquid height in the sprue and in the mold increased together to the level of the middle step. Flow would then occur through the middle step as well as the bottom step. In the same way, when the top step was passed, all three steps should feed.

It is to be expected with conditions of slow steady flow that momentum of the down-stream is unimportant. When flow is rapid, as it is in most pouring operations, it is to be expected, however, that the momentum of the stream will carry it past lateral openings (follow rectilinear path of least resistance) to the bottom of the sprue. The metal will then feed most heavily through the bottom step through the entire casting operation unless sharp angles, baffles, or entrapments are incorporated into the system to dissipate momentum and pressure effects.

To recapitulate, pressure effects (PV) are developed

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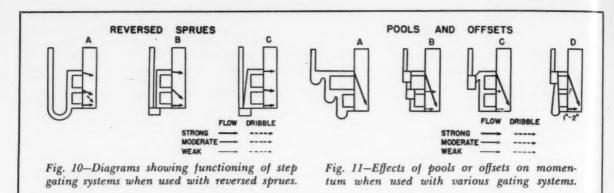
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by the transfer of kinetic energy (Mv²) to energy of elastic compression. Preferential impingement near or at individual finger inlets results in variations of pressure at these openings, hence to non-uniform feeding from the various fingers. It is possible to counteract such non-uniformity of feeding by varying the individual dimensions of the fingers or the finger angles relative to the runner.

However, this may be done more readily by preventing the development of differential pressures at the finger inlets by the expedient of enlargements in the runner system which result in dissipation of momentum prior to entry into the fingers. This method embodies control of the generation of pressure. Control of pressure dissipation by critical design of finger inlet geometry is a considerably more difficult procedure.

Mold Entry Phase of Pouring—Development of Turbulence: The design of gating systems, as has been pointed out in this and a previous report¹, has a profound influence on the turbulence in a casting. Previous work showed the existence of at least two modes of turbulence in finger gates. These were swirling and rolling turbulence. The investigations recorded here show that a third form, called here rocking turbulence, exists. This type of turbulence results from intermittent feeding through finger gates.

The effects of turbulence, per se, on the properties of castings are not known. Such a study was outside the immediate scope of this investigation. Inasmuch as mold turbulence proved to be an excellent indication of feeding conditions it was used chiefly in this study as a means of determining the uniformity of feeding through the finger. The fact that mold turbulence depends on the gating system indicates that such turbulence can be controlled by the feeding sequence and pattern.

Conclusions

The results of these studies of the flow of molten steel in gating systems in terms of established hydrodynamic concepts requires cautious interpretation, inasmuch as the idealized conditions of lamellar flow in true pressurized systems are seldom met. Actually, the flow conditions are continually changing during the course of pouring. The initial entry of metal into the gating system approaches free fall, but as the system fills, a ferrostatic back pressure is developed and the action approximates that of a pressurized system.

The conditions of first-stage pouring place a predom-

inate importance on momentum and related pressure effects. This stage of flow should therefore be dependent on the specific densities of the liquid.

Second-stage pouring occurs through the development of back pressure; during this period the flow approaches that of a pressurized system. In this stage momentum-pressure effects are reduced and density differences should necessarily be less important.

These studies have shown that uniform or sequence flow may be obtained (1) by elimination of pronounced momentum-pressure effects by use of enlargements, and (2) by control of momentum-pressure effects to aid proper feeding by sharp angles, constrictions or increased flow distance.

Finger gates of special design are required to secure uniform flow. These gates either split the fluid stream into equal parts or absorb the momentum in the runner. Absorption of momentum is most effectively obtained by the use of pools in the runner and with lesser success by bending the runner away from the casting, or by using a tapered runner.

Commonly used step gates cannot be relied upon to produce sequence flow except by modifications using special devices. Studies of single junction and multiple junction steps in step gating show that inclination of the bottom step promotes sequence flow for the two bottom steps. Tapered sprues may also be used, but these are exceedingly critical and no example of full sequence flow was actually obtained by such devices in this investigation.

Turbulence arises from the flow patterns produced by the gating systems. In the investigation to date, three forms have been observed. These are (1) swirling, produced by unequal feeding; (2) rolling, produced by uniform feeding and (3) rocking, produced by intermittent feeding.

Acknowledgments

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SALVAGE OF CASTINGS BY WELDING OF DEFECTS

By

G. E. Bellew*

ABSTRACT

Equipment, materials, electrodes and techniques for arc welding of mild steel, low-alloy steel, and stainless steel castings are described on the basis of economy and efficiency, and adaptation to foundry requirements.

Repair welding of gray iron castings, by various arc methods and by oxyacetylene welding is discussed in some detail.

Welding on non-ferrous castings is discussed, including not only the older methods, but the new inert-gas-shielded arc-welding processes which are rapidly beginning to find use in foundry work

The paper constitutes a review, an over-all description, of present-day thinking and practices for repairing defective castings by welding.

No one has yet found a way to wipe out the problem of voids and similar defects in castings, whether they are steel, iron, bronze, aluminum or other alloys. A certain proportion of the castings come from the molds with these flaws, which do not affect strength or usefulness as a whole too seriously, but they do not look well, and must be filled in if the casting is to be used. Fortunately, by means of welding, such flaws can be corrected with deposits which will usually meet the requirements of strength, color, and other qualities, at a cost level which makes it highly economical to carry out this salvage operation.

Such work has been more or less standard practice for many years. As the cost of producing castings has gone up, so that the rough piece represents an everincreasing investment in time, material, and labor, the practice has become more and more prevalent.

A tremendous volume of experience has been built up, and in some types of operation, the job is a routine stage of cleaning and finishing. In others, however, it is still being done all too frequently according to the ideas and standards of twenty years ago, largely because the accumulated know-how has not been sufficiently coordinated, sifted, and presented to the foundrymen at large. Today we can well afford to take a two-fold view of the possibilities, based upon:

1. The basic thinking about the use of welding for casting salvage, developed from the experience of sev-

eral decades, and-

2. The important new developments in welding which are applicable to casting salvage, particularly in the high-alloy ferrous and the non-ferrous fields.

One of the most important factors in the overall picture is that of costs, which are difficult to calculate accurately in an operation where there is no practically measurable unit of work. In fabrication operation, we have "x" feet of weld, of "x" size, using a specified method and material, and a per-unit cost can be quite readily figured. In salvaging castings, we may have to deposit anything from a few drops of metal up to several pounds on the same part, depending upon the extent of the flaws. We can only roughguess our costs, therefore, for a given operation, usually as we acquire experience with various types of work. We can, however, by investing in the most suitable equipment, by selecting materials well adapted for the work, and by the use of properly trained and skilled operators, hold the cost, per pound of metal deposited, or per hour of welding time, to a reasonable minimum. In other words, if we set up a thoroughly efficient basic operation, and if we provide adequate manpower to run it, we have the basis for good economy.

The rest of the economy will result from sound selection of economically salvageable castings. The greater part of the time, this is an open-and-shut proposition-the part is obviously either salvage or scrap. In some cases, however, it will require a fine eye to determine which way the casting goes-knowledge of the time required, of the cleaning necessary, and of the type of results that will be obtained with a particular kind of defect in a particular kind of material. Often in such cases the production schedule of the shop will be a determining factor. If casting production is heavy, but the welding shop not too busy, doubtful units may be salvaged more economically than when welders are rushed and the casting floor is slack. These are the problems of management and supervision, and they will be far easier to decide wisely if welding facilities are well organized and efficient, and if those responsible understand clearly just what welding can do for them.

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Fig. 1-400-amp DC welding machine for foundry welding.

salvage of casting today, as in the past, is in steel castings, better material and methods for cast iron have evolved, and excellent means are available for stainless steel, aluminum, magnesium and bronze as well. New materials, equipment, and processes have greatly widened the usefulness of welding in foundry production, while older methods have been improved and widened. If these techniques were fully used today, the castings industry, in almost all its branches, could gain highly worthwhile new savings, and increase its overall efficiency to a marked degree.

Steel Castings

Arcwelded salvage of steel castings (Fig. 3) is the most widespread, and so far the most generally successful application of welding to repair castings defects. Basically it can be a simple, readily executed job, using standard types of equipment and materials and standard techniques. In spite of its long history and wide application, however, it is still beset in many cases with problems and minor difficulties, and in too many specific applications there is room for considerable improvement.

In the past, equipment used in most foundries for steel arc-welding has been based on the direct current generator (Fig. 1) which was for many years the all-purpose welding machine. There can be no real quarrel with this arrangement, but there are many considerations which recommend foundrymen to think in terms of the modern alternating current welding transformer for their purposes.

AC machines (Fig. 2) are cheaper to buy, generally speaking, and to run. They are less affected by dust and dirt—still to some extent a plague of ferrous foundries, in spite of great improvements in recent years—because they have no moving parts, and are susceptible to more nearly dustproof design. Finally, they offer possibilities of easier and more comfortable

welding characteristics, particularly in the reduction of the annoying phenomenon of "arc blow"; and in the wide range of highly satisfactory AC electrodes which are available today.

One element which should be given important thought in selecting a welding machine is its size. It should be kept in mind that in foundry repair-welding the rate of deposition is an important cost factor; that downhand welding with large diameter electrodes is the best way to increase this rate; and that a highcapacity machine with a little size to spare is cheaper to maintain and operate than an overloaded machine of smaller capacity. In the majority of foundry work, for instance, 5/16-in. and 1/4-in. electrodes will be the most favorable, both for deposition and cost-per-pound of rod, and to use them effectively, a capacity in the range of 400 amperes should be available. Where lower currents are needed for smaller electrodes, the larger machines will still provide adequate adjustment. Thrift, unwisely applied in the purchase of the welding machine, may lead to hidden but expensive waste in shop operations, and only those who may have experienced the difference in cost when larger machines replace inadequate ones can really see their importance.

Corollary to adequate welding machines, suitable accessory equipment is important. Adequate cables for the currents to be used, with properly made up and used connections, are essential to efficiency and economy. Heavy duty electrode holders, capable of "running cool" with the large diameter electrodes, and built for long life under rugged conditions, will increase not only economy and efficiency, but safety and

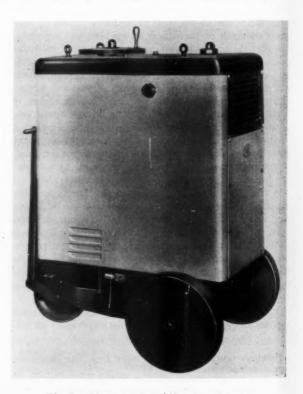


Fig. 2-400-amp AC welding transformer.

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d nd nd comfort for the operator also. Provision of adequate safety measures—helmets, goggles, fume collectors, screens, etc., and of safe and well built equipment is of key importance, and should be made a part of the basic equipment picture rather than a casual afterthought.

Mild Steel Electrodes

When we consider the question of electrodes, we must necessarily separate the steel category into its three chief classes for castings—mild steel, low-alloy steels, and the stainless types. While the basic considerations of equipment, planning, and practice are the same for all, the electrode pictures are quite different.

Mild steel and low-alloy electrodes are available in a bewildering number of types, sizes, analyses, and prices. It is a relatively simple task, however, to arrive at a more limited field from which to choose. One of the first steps is to consult a qualified technical representative or a first-class electrode manufacturer. Such advice costs nothing, is always available, and can be of great assistance and value, either in specific recommendations for a particular job, or in general recommendations of a selection of suitable materials.



Fig. 3—Typical weld-salvage operation on a steel casting.



Fig. 4—Teaming of oxyacetylene flame-gouging (left) and arc-welding (right) on steel casting defects.



Fig. 5-Aluminum castings above, were fabricated by inert-gas-shielded tungsten-arc welding.

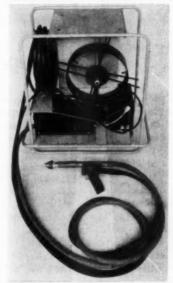


Fig. 6-Equipment for inert-gas-shielded metal-arc welding.

Generally, the electrodes most suitable for steel casting repair will be found in the E-XX10, E-XX12 and E-XX16 categories. The E-XX10 types include the "all-purpose" steel electrodes, and these will cover a great share of the general run of repairs. The E-XX12 class are largely characterized by very high deposition rates, and are an excellent choice where sizeable voids must be filled, and where mechanical property considerations are such that high speed and time savings can be made the first objective. Where mechanical properties are high, and best quality is the first objective, the E-XX16 class, the low-hydrogen rods, are the most likely choices. They are also useful with tricky or dubious analyses, in cases where weld cracking might be a potential problem. (More detailed information can be had by reference to the following ASTM specifications: For mild steel-A-223-48T; for low-alloy steel-A-316-48T, and for stainless steel-A-298-48T).

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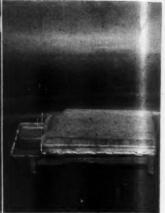


Fig. 7—Aluminum alloy 108 castings, prior to fabrication by inertgas-shielded metal-arc welding.

Fig. 8-1/2-in. square butt joint being welded at 24 in. per min.

Fig. 9—Finished welded aluminum castings. The part is tested at 15 psi hydrostatically.

There is one problem in the choice of electrodes for welding steel castings which is a recurring source of difficulty. Too many users, in specifying the electrode requirements, try to use weld metal which will match the casting in analysis. There are good reasons why a casting has a certain analysis-among them are the mechanical properties desired, but many of those reasons have to do with furnace and casting practices. We come up with a casting of "X" analysis, with "Z" mechanical properties, to do a specific job. It also has metallurgical properties which are present because the metal has been melted and cast according to certain proven procedures. If we take a rod of this analysis, and make an electrode of it, the chances are better than average that we have, at the best, a mediocre electrode, for just as casting metal requires certain characteristics for satisfactory results, welding electrode metal must be properly balanced for good operation. One result of the attempt to match analysis exactly is the frequently required use of pre-heating and post-heating in welding high carbon analyses-a costly procedure, and one which is not always necessary if electrode selection is approached from a different view.

It is more satisfactory for weldability, for economy, and for quality of the results, if the standard set for the electrode is that of mechanical properties rather than analysis. This permits the use of a rod which will perform satisfactorily in welding, and which will deliver a deposit equal to or better than the rest of the casting.

In general, fast deposition is a desirable thing in repairing castings, and for this purpose, the largest electrode size practical is the choice for most such work. With the larger diameter rods, high currents are used, and as previously mentioned, high capacity, heavy-duty equipment is required. The only important consideration of technique, other than standard welding practice, is the cleaning of the area to be welded. It may be ground, chipped, or oxyacetylene flame-gouged, but it should be cleaned thoroughly, down to sound, clean metal, before welding. In fact,

many foundries make the preparation of defects by flame-gouging (Fig. 4) a separate operation on a production basis.

In broad terms, weld repair of steel castings requires, in addition to the proper equipment, accessories, and manpower, sound techniques and a wise approach to electrode selection. Given this, it should be a straight-forward and satisfactory operation, paying handsomely in savings and profits for a sensible investment.

Stainless Steel Electrodes

All of the same basic considerations for mild steel apply to stainless castings. Electrode selection, however, is on a considerably different basis.

Here we are in a field where alloy content, and analysis, are of paramount importance. In stainless steel, there is a fairly important reason for almost every point of almost every element present. Therefore, with these materials we look for an electrode which will match, as closely as is possible, or surpass in important respects, the base metal alloy content. Modern stainless electrodes are so composed that there is little or no trouble in procuring a deposit matching the desired analysis, except for occasional difficulty with the stabilizers used in some alloys. It is usually safe, therefore, to consider that an electrode of suitable nominal analysis will deposit satisfactory weld metal.

It is always a good approach, however, with these materials, to make full use of the technical advice of the electrode supplier. Certain special considerations may enter into a specific job, for instance, where such help is invaluable.

Most modern stainless electrodes are available in both straight DC and AC-DC types. The choice between them will depend upon whether the lower price of the DC rod or the greater welding ease of the AC-DC type is more valuable to the individual user.

One further aspect of working with stainless steel should be kept in mind. A stainless casting represents a considerably more costly loss than a mild steel casting, if it is scrapped. It is worth while to give stainum

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less welding even more carefully planned equipment and methods, better qualified operators, and thorough planning, because the potential profit or loss from the operation is so much greater.

Cast Iron

Gray iron has been welded in many ways, with many techniques, for a long time. It has never been an ideal material for welding, and there is no simple or easy method whereby welding results in a class with those obtained on steel or other metals can be achieved. We must always sacrifice something to weld it—machineability, mechanical properties, appearance, or the cost of elaborate preheat and postheat operations. Probably, unless someone can change the basic reaction of gray iron castings to the kind of heat used in welding, it will always be a problem.

We do, however, have ways of welding gray iron, and they are sufficiently varied and satisfactory that most, if not all, defective castings can be salvaged.

Where results are not too critical, the arc can be used. For defects which require only filling-in, and where they do not have to be machined, steel electrodes can be used satisfactorily. Color match is good, and the operation is inexpensive. Where machineability is important, and where preheat is undesirable or impractical, the nickel electrodes with shielded-arc coatings are used. These rods are comercially pure nickel, used on AC or DC interchangeably, with low currents and a soft arc. This weld is more nearly what we think of as a "braze-weld," with little or no penetration into the base metal, and there are minor differences in mechanical properties, most important of

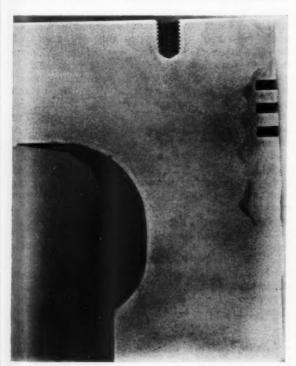


Fig. 10-Etched section of deposit on a Diesel engine piston of high-silicon aluminum alloy.

which are lower hardness, higher strength and ductility. Such welds are, however, fully machineable, color match is good, and in many cases they do the job with gratifying results.

For quality work, where requirements are critical, there is no substitute in cast-iron welding for oxyacety-lene torch welding with cast iron rods and careful pre-heat with slow cooling. Using the right welding procedure, taking care of every possible problem, this method produces absolutely sound welds, matching the base metal practically identically. They are fully machineable, and have excellent mechanical properties. Actually, in this method, we closely simulate the casting process itself—with the weld metal molten, and a fairly even temperature gradient in the surrounding area; it is as if the weld were only the last of the casting metal to freeze.

It probably should be included here that both steel and stainless filler rods may be used with oxyacetylene methods for special jobs. They are, however, too expensive and troublesome, in most cases, to be considered, and their use is too specialized for more than a mention.

To sum up, gray iron may be welded with the arc, using steel for lowest cost non-machineable welds or nickel for machineability; but for quality and machineability, the higher cost method of careful preheat and oxyacetylene welding with cast-iron rod is recommended.

Non-Ferrous Castings

Coated electrode arc welding is, by and large, inferior to the torch on brass, bronze, aluminum, magnesium and the related casting alloys. Standard methods exist for handling these materials with the flame—good quality fluxes, filler rods, and other materials are readily available—and castings can be repaired with reasonable ease and success in most materials.

The brasses, because of their zinc content, are not so satisfactorily weldable as the other non-ferrous casting materials, and none of the non-ferrous metals are either cheap or easy to weld with satisfactory results by the older basic methods. This is as true in production fabrication as it is in casting repair, and it has led in the last ten years or so to the rapid development of two arc methods which are changing the whole pattern of fabrication of these alloys, and of stainless steel, and which offer great promise for application in foundries specializing in stainless steel or the non-ferrous alloys. Their field of use in the castings indus-



Fig. 11—Test-deposit specimen of aluminum bronze with inert-gas-shielded metal-arc process shows good surface appearance (left) and soundness of the metal in the polished section at (right).

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try is somewhat different in some respects than in fabrication, but their basic characteristics remain the same.

One of these processes, the inert gas-shielded tungsten arc process was used extensively during the war for welding magnesium and aluminum. Since the process eliminates the fluxes necessary with other welding techniques, it was distinctly helpful in war-time welding activities. In the post-war period the applications of this process were extended to stainless steels, nickel and its alloys and copper and some of its alloys. In the field of cast metals it finds use particularly on magnesium and aluminum and alloys of these metals.

The process is simple, using a non-consuming tungsten electrode of fairly small diameter, with a blanket of inert gas, such as helium or argon, surrounding the electrode, the arc, and the weld pool. With this inert gas shield blocking off atmospheric attack, and with suitable electrical characteristics, this process makes it possible to weld aluminum and magnesium without flux, producing a sound, clean deposit of excellent appearance and properties. Filler metal is fed, and the electrode holder is manipulated in much the same manner as the rod and torch in oxyacetylene welding.

With this process, highest quality results (Fig. 5) can be obtained on the light metals with excellent mechanical properties and considerable flexibility of operation. While the process is not the cheapest alternative to use, it is the recognized ultimate in quality, and it is economically efficient where the workpiece warrants such standards.

The second of the new processes is the inert-gasshielded metallic-arc process, which is applicable to aluminum, stainless steel, and aluminum bronze and other low-zinc bronzes. With this method, a continuously fed filler wire acts as the electrode, blanketed in inert gas such as helium or argon, and the basic advantages of flux-free welding are also present. In addition, by using high current densities on small diameter wires, and high wire-feed speeds, deposition rates and efficiency are remarkably high.

One type of equipment for this process consists of a machine (Fig. 6) to hold and feed the filler wire, a flexible cable connection to the welding "gun," the gun itself, a shielding gas supply, and a DC welding generator. It is the most complex equipment for any of the processes we have discussed, but the resulting welding operation is one of the simplest, and certainly the most rapid of them all. Inherent automatic regulation of arc length is one feature; another is that the welding can be carried out in vertical and overhead as well as flat and horizontal positions. The arc is visible, and clean-up is almost completely eliminated.

Aluminum Casting Welding

This process has been used to weld castings, particularly in aluminum. The job illustrated in Fig. 7, 8, and 9, while it is not a repair job, demonstrated its

facility for welding aluminum alloy 108. In Fig. 10, we have the etched cross-section of an aluminum Diesel piston repaired with the process, which gives a clear idea of the quality of the weld metal deposited as compared to the original casting. In both cases, the savings obtained by the great increase in deposition rate and efficiency over the previous methods used (inert-gas-shielded-tungsten-arc) were considerable.

With stainless steel castings, this process builds its primary attraction on the speed and flexibility with which it can be used. It is so much faster than conventional stick-electrode welding, and with the added saving of eliminating clean-up and slag-removal operations, reduces the working time so sharply, that appreciable cost savings can be realized. In the case requiring the deposit of between 6 and 7 lb of weld metal, with the conventional electrodes about 15 lb of rod and 6 hr working time were required. With the inertgas-shielded metal-arc process, only 7 lb of wire and $1\frac{1}{2}$ hr of time (gross) were needed.

Results in welding copper base alloys have been satisfactory. Aluminum bronze is deposited with ease, and the quality of the deposits equals or exceeds that obtainable with other techniques (Fig. 11). Similarly, welding the silicon and low zinc manganese bronzes have yielded excellent quality welds surpassing previous results. While much of this work has been performed on rolled sheets, sufficient work has been done on cast metals to indicate definite applications in this field.

Summary

It seems, at first glance, difficult to generalize about the welding of castings in so many materials and different ways. There are, however, two generalities of primary importance. One is that the equipment and facilities, manpower and techniques, for the work must be planned as carefully and efficiently as possible. It should be considered completely a production operation. Capacities, sizes, and type of equipment and materials should be ample-overworked facilities and men are worse than inefficient, they are unsafe. The second generality is that progress is being made, steadily and rapidly in welding methods and techniques, and no established operation, however satisfactory, should remain "as is" for too long a period. We do not know, for instance, exactly how great a future the new inert-gas-shielded metal-arc process may have in casting repair operations. Applied to mild steels as successfully as it is to stainless, it could revolutionize foundry thinking about welding steel castings, and make much of this paper obsolete. Until that time, however, sound planning in the use of established methods on steel and cast iron, and wise application of the newer methods to non-ferrous alloys and stainless, give the foundry ample opportunity for a low-cost, high-quality job of salvage welding.

COLD FORMED FLEXIBLE "PRECISION" PATTERNS AND CORE BOXES

By

R. B. Wagner* and J. E. Wiss**

ABSTRACT

Flexible precision patterns and core boxes are ideal equipment for the forming of metal casting plaster, investment plaster, complex wax or other thermosetting positives. Properties of flexible equipment, which make it desirable. Discussion of previously used materials. Step by step description of one method of making flexible cope and drag multiple patterns in a new type of synthetic rubber, starting with a single loose piece split pattern. Illustrations showing steps involved in this new approach, using cold formed synthetic rubber compounds with setting properties similar to plaster of Paris. Review of applications already proved and future possibilities in the precision casting field.

FOR MANY YEARS reproduction of complex metal surfaces in investment plaster molds of whatever kind, has been limited to the "lost wax" process; to multiple piece rigid pattern assemblies which can be readily withdrawn from the investment plasters; to molds taken from thermoplastic resilient surfaces, such as the plasticized polyvinyl chlorides; to "skin" type molds made from dried films of rubber latex or synthetic latices; or, rarely, to flexible molds made of glue gelatin.

There have been many production attempts to use commercial metal casting plasters directly on pattern equipment which is satisfactory for sand molding. Only a few of these attempts have succeeded. In the pressure cast matchplate field, operating routines have been worked out, which permit direct multiplication of single loose patterns into multiple gang matchplates or similar core box equipment.¹ In the Bean process,² development of the thermoplastic pattern in polyvinyl chloride has reached a high order of utility in the non-ferrous field. The Capaco³ process, likewise, using a different pattern approach, also permitted extremely close dimensional tolerance castings to be made from investment plaster molds.

Properties of Plaster in Patterning

Plaster molding, to the sandmolder, presents a number of problems of an unfamiliar nature. It is difficult to cast and strip it under vibration, as with sand, without damage to the original smooth cast surface. Generally, it varies from a thin slurry to a rather viscous fluid at the time of placement on the pattern. It must be permitted a sufficient time to harden before removal from the pattern. Before the hardening period, unless the pattern and the flasking have been kept leaktight, leaks and spillage have rendered the moldmaking process a messy operation.

When poured over large areas, the property of gypsum plasters⁴ of expanding slightly during the setting period has been undesirable in metal casting mold forming from rigid pattern equipment. The setting expansion either locks the plaster tightly to the pattern, or over wide areas causes undesirable bowing and bulging when the plaster is rigidly flasked around the pattern, unless the flasking is so formed as to give during the setting expansion, and the pattern itself is so well drafted that the expanding plaster can spring loose during its set.

Practice in An Allied Field

In a paper presented to the American Ceramic Society⁵ the authors reviewed similar difficulties in the preparation of gypsum plaster molds for use in the ceramic industry. These molds in many respects must parallel the properties of foundry molds, with some exceptions, naturally, based on the fact that clay in all forms from thin slurries to plastic masses, is shaped on them by different forming methods. The molds, however, must parallel in surface quality, at least, and dimensional accuracy, the properties desired in foundry molds. In the ceramic field the types of gypsum plaster used are not compounded for heat resistance and "one time" use, but must resist abrasion and solution by water during repeated use.

In that paper a new method of forming the thermoplastic polyvinyl chlorides⁶ was described in full. A series of photographs was presented picturing the procedures involved in developing a master flexible pattern from a polymercaptan synthetic rubber. Since the ease of forming this latter material at room temperature was as simple as developing a plaster casting, it was felt that a similar approach to the foundry pattern field would offer a new approach to

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the production forming of gypsum plaster investments, as well as those investment plasters with setting properties similar to gypsum, such as portland cement bound refractories and the various silicate and titanate investments for the casting of ferrous alloys.

Properties of Flexible Pattern Equipment

In the forming of investment plaster surfaces, the flexible pattern offers a number of advantages:

1. Parting compounds can be completely eliminated. This elimination assists greatly in producing a mold with no contaminating lubricant or parting,



Fig. 1-Starting Fig. 2-Impression mold of pattern in alpha gypsum.



Fig. 3—Pouring the synthetic rubber duplicate.

which may later cause gassing or blowing into the metal, if not completely removed in the dehydrating process.

2. The investment plaster can take its complete setting expansion without distortion,

3. Minor undercuts and backdrafts can be extracted readily without damage to the plaster surface,

4. Pattern plate equipment of the core box type with pattern face and flasking side walls can be made in one piece, eliminating the dangers of leakage and cleaning up the operation of mold forming,

5. With proper selection of the flexible material for the job in hand, thousands of accurate plaster impressions can be made,

6. With plasters of the high permeability type described by Miericke and Johnson⁷ in 1948, smooth unbroken casting surfaces can be developed and handled readily from the pattern. This has been a difficult operation with rigid equipment.

Forming methods currently in use covering gelatin, rubber latex,8 and thermoplastic polyvinyl chloride,9 were rather fully discussed in the ceramic paper mentioned above. With all prior materials of this type, it has been necessary to compensate either for the evaporation of free water (rubber latices) or for the thermal contraction of the thermoplastic materials hitherto available.10

One recent approach with suspensions of polyvinyl chloride in plasticizing liquid has been attempted also. These latter must be heat treated in order to develop them into gels. When this is done, the same type of correction for thermal contraction needed for the thermoplastic materials must be given, where faithful dimension transfer is required.

The authors, in their constant approach to this problem, have made a rather large number of shop trials on methods of duplicating in these various materials. One interesting by-product of these trials has been the confirmation of the belief that with any material used, the skilled craftsman can best be trained in a shop by other craftsmen who have already learned the art. The simplest cycle of operations so far developed and the one most rapidly adaptable to the average foundry or pattern shop has been found to be a procedure adapted around the ease of handling of gypsum cements with their controlled setting time and setting expansion, as well as strength. It has been found that with the cold setting polymercaptan synthetic rubbers,11 manipulations similar to







Fig. 4-Follow boards and duplicate patterns.

Fig. 5-Patterns mounted in follow Fig. 6-Grouted up original pattern boards, positioned.

plate, rubber patterns in position.

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that given to plaster will develop vulcanized resiliently flexible duplications at room temperature handling—thus eliminating all need for dimension compensation due to heat shrinkage.

Cope and Drag Flexible Plates from Single Pattern

As a typical example, development of a pair of



Fig. 7-Pattern plate with loose rubber patterns removed.

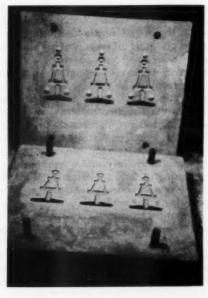


Fig. 8-Matching half master cavity mold.

cope and drag plates will illustrate the process cycle. There has been no attempt in forming these plates to indicate the location of sprues, gates, or vents in the finished investment plaster, since these as well as the effects of various types of metal alloys require judgment and study at the point of use in the foundry.

Figure 1 shows a photograph of an ornamental key, a split pattern pinned together at the parting line. The pattern is first bedded in modeling clay to the parting line; the clay extending 1 in. to 11/2 in. beyond the parting line, as indicated. A metal collar (flask), preferably thin strip lead or sheet metal, bent around this clap; its height at least 1/2 in. above the highest part of the pattern. The pattern and the clay surface are then lubricated with a suitable parting compound. Over this lubricated area within the flask, a thin fluid mixture of low expansion alpha gypsum is poured and allowed to set. When set, the assembly is turned over, the lead collar and the modeling clay removed from the drag half. At least three tapered depressions, 1/2 in. diameter by 1/2 in. deep, are countersunk around the edge to act as locating keys.

The block is reflasked with strip lead, the assembly lubricated with suitable parting compound, and a low expansion alpha gypsum matching half poured over the drag half of the pattern, taking precautions to entrap no air on the pattern surface. The assembly thus developed with the low expansion gypsum cement is an exact (within 0.003 in. per ft) impression of the pattern (Fig. 2). Thus we have a single two-piece mold or "core box" of the pattern. Onehalf of this mold is removed from the pattern-either cope or drag. The procedure to be followed requires the production of three synthetic rubber duplicates of the pattern, so two more similar sections are cast to provide three follow boards (Fig. 4). Any one of the three follow boards is now fitted against the original half, from which the starting pattern has been removed, and a pouring sprue developed to extend into the pattern cavity mold.

Into this cavity mold, after suitable lubrication to provide clean parting, the cold setting polymercaptan rubber is poured (Fig. 3). (Stearic acid or petrolatum thinned with kerosene, or silicone mold oil in very thin layer, all provide equally good partings.)

After each duplicate has set (Fig. 4), it is removed from the original cavity mold and placed in its proper follow board (Fig. 5). The three rubber duplicates



Fig. 9-Pouring the synthetic rubber pattern.



Fig. 10-Showing imbedment of sisal fiber into partly cured rubber.



Fig. 11-Pouring the supporting backup low expansion plaster.

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so formed, supported in the three follow boards, are placed as shown in the photograph, Fig. 5, being lined up in the most practical manner for production. A suitable sized flask is placed around these, the top of the flask preferably level with the top of the follow boards, and the size of the flask most practical for production. A fluid mixture of low expansion alpha gypsum is poured around the three follow boards and flush with their surface; this grouting serving to hold the three exactly in position. The surface of the grouting is faired as smooth as possible with a sharp steel straight edge fairing tool (Fig. 6). The gypsum cement surface, as well as exposed surface of the synthetic rubber is then lubricated with parting compound, a second flask is placed above the first, and filled to at least 1/2 in. higher than the topmost part of the exposed patterns, with low expansion gypsum

This pour is screeded off and struck level with the top of the second flask. After the gypsum cement on this pour has set, the flask is removed and the single flexible patterns taken from their supporting follow boards (Fig. 7). The two low expansion gypsum pieces developed may be called the "master cavity mold." They can be kept as reference molds for the duplication in flexible materials of the final working pattern (Fig. 8). In storage, the master should be given a very thin coat of vaseline or petrolatum jelly, and the individual flexible inserts kept mounted in the closed master mold for protection against dirt, efflorescence, and mechanical damage.

In the development of the final working pattern with polymercaptan rubber, the surfaces of the gypsum cement are given a thorough coating of liquid potash soap, brushed and sponged to develop the typical soap shine on wet plaster. All traces of the surface free soap are removed, and the master mold pattern is ready to receive the flexible molding compound. Onto each of these assemblies after fitting proper flasking, approximately 3/8 in. thickness of fluid cold setting compound is poured (Fig. 9). When this batch of compound has set, another smaller mix is made, poured over the previously set compound to a depth of 1/8 to 3/16 in. Into this fluid, long sisal fiber (Fig. 10) is imbedded by pressing over the entire area and permitted to remain until the compound has stiffened sufficiently to remain in place,

entire area and permitted to remain until the compound has stiffened sufficiently to remain in place,

Fig. 12-Finished flexible faced pattern plate.

during the pouring of the backup slab (pattern plate) of low expansion plaster (Fig. 11). The backup slab will fill between the fibers and the heat of setting of the gypsum cement develops the final set on the molding compound. The backup slab affords rigid support for the flexible surface and the fiber affords the permanent mechanical tie to the synthetic rubber. In addition, and of equal importance, it has been found that the low expansion plasters poured onto partly set synthetic rubber of this type, develop good surface to surface bond. This backup slab is finished to a true level surface, to assure pattern trueness in use and in storage. It may be mounted on a metal plate to withstand extreme shop abuse.

Removal of Finished Plate from Master Mold

After these assemblies have been completely cured, preferably overnight, and the setting heat of the backup has completely cooled down, the rubber pattern is separated from the master pattern mold. Because of the extremely close lay of the flexible to the master pattern mold, it has been found that judicious use of compressed air to break the surface of contact works well. It is possible to blow compressed air through the back of the master pattern mold by drilling a small 1/8 to 1/4 in. hole approximately 1/9 in. into the back of the master and forcing compressed air through a tapered nozzle down into this hole. There is sufficient air and free water "blow through" the plaster to permit ready parting. When this is done, the hole must not extend clear through the master pattern mold, or damage to the finished rubber pattern surface will develop. The resultant production from each master pattern half mold is the finished flexible faced equipment shown in Fig. 12.

The thin upright sections of the pattern are completely flexible and for sand molding could not be trusted to retain their position without distortion. For the forming of fluid investment plaster, however, where liquid pressure works equally on all sides of the protruding thin sections, equilibrium exists and distortion does not occur.

Figure 13 shows the same operation in which the side walls, as well as the pattern face, have been developed over the master pattern mold. The construction might be called "flexible core box" construction, since flask and pattern are in one piece.



Fig. 13—The same assembly, with flexible synthetic rubber flashing, made as core box construction.

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This is the most desirable form so far developed. It is possible, by using tapered snap flasks, to hold the exterior of the flexible walls in true position until fluid plaster fills have set. However, since the exterior of the mold generally has no need for exact dimension control, the use of the flexible wall alone without flasking is sufficient in most mold forming operations.

Resume

The authors feel that the method of mold forming for investment plasters is a new approach and eliminates many of the normal foundry objections to the use of plaster molds. It is felt that the method of converting metal or wood equipment into flexible equipment described in this paper, will permit the conversion of any currently used pattern where better surface and dimension detail are required. It is recognized that all present day sand patterning is so drafted as to be readily removable from sand, and frequently has added to it extra thickness and dimension for final machine finishing, which the plaster type of casting is expected to eliminate.

It is strongly urged that where new equipment is being made for use with investment or "metal casting" plasters, the original pattern cavity (that is, the master mold) wherever possible, be developed directly in plaster or gypsum cement, from which there is but one step in translating directly to flexible equipment. Where the complexities of a gang pattern make this too costly, any single pattern may be multiplied into synthetic rubber duplicates in the manner outlined, low expansion alpha gypsum master mold patterns. formed as described, and cope and drag plates or core boxes, as pictured, can be developed at will.

Using the techniques described with harder and more abrasive plasters, than commonly used metal casting plasters, experience over the past three years indicates that a life expectancy of from one thousand to three thousand plaster molds can be taken before repair or replacement is necessary. Further experience indicates that the very permeable gypsum plasters described by Miericke and Johnson can be handled with relative ease and to extremely good surfaces without using greasy or oily parting compounds. This has eliminated the tendency for separator runs and surface defects on the plaster caused by parting compounds, as well as metal defects resulting from excess parting compound causing gas blows in the metal during the casting operation.

Under extremely severe conditions of service, the plasticized polyvinyl chlorides formed against the master pattern mold by the vacuum process, give equivalent or longer service, depending on the type of thermoplastic used. Where this type of flexible is used, it is preferable to develop it over a gypsum pattern mold; the pouring of these hot melts calls for special techniques to compensate for thermal contraction, and for directional gelling or "freezing" of the thermoplastic. The vacuum technique of forming⁵ has proved its worth where the special properties of the thermoplastics are desired. The cold setting synthetics in this case are extremely useful as intermediate patterns in developing the master pattern mold in plaster.

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THE ENGINEERING STUDENT AND SUMMER FOUNDRY WORK

By N. J. Stickney*

Introduction

RECOMMENDATION HAS BEEN MADE that Foundry Educational Foundation scholarship recipients be required to work in the foundry industry for at least two summers prior to graduation. Under the impetus of this organized program to develop engineering students for future work in the foundry field many engineering students have become aware of the possibilities in that field of endeavor. They have voluntarily entered into summer work in foundries to supplement their academic work. Increasing numbers of students will therefore participate in each succeeding summer work program for some time to come. The foundry industry as well as the students benefits from this practice.

This paper contains a discussion of the 1949 summer work program conducted by the Foundry Educational Foundation. It attempts to present the benefits realized by industry and student, and offers suggestions which might aid the foundry and the student in realizing the fullest possible advantages from a summer work program.

Survey of Students

A survey of students who worked in foundries this past summer (1949) was conducted by the author with the aid of the Foundry Educational Foundation. Questionnaires were voluntarily returned by 60 men, approximately one-half of the total number of college engineering students participating in summer foundry work. A compilation of the answers to questions pertaining to type of job experience gained, opportunity to observe general foundry operation, the degree of physical labor involved, amount of cooperation received, and possible improvements in future summer work programs, forms the basis for this paper.

Before entering into a discussion of the past summer's work it would be well to review some of the reasons for the active interest and excellent participation of the foundry industry in providing the opportunity for summer placement of engineering students.

Possible Benefits to Foundry Industry

There are a number of ways in which a foundry can benefit from a summer work program. The foundry, should it have in mind an addition to its staff, has an excellent opportunity to observe the student (or prospective employee), his work habits, his ability to get along with regular foundry workers and staff, and his attitudes and interest in this particular foundry operation. Summer work provides foundries with an opportunity to pre-select personnel for the future. The student has well in mind the type of foundry operation and location which he would prefer by the time he attains senior status in school. Because of this opportunity for discovery of mutual interests, a considerable number of the participants in this past summer's foundry work program have been offered permanent positions in the foundries where they worked.

A foundry employing a graduate engineer with previous foundry job experience gained by summer work might also decrease the training period required to adapt the man to his place in the organization. The average summer period results in a considerable fluctuation in foundry help as a result of hot weather absenteeism and employee vacations. Students, many of whom have had previous foundry experience, can provide the labor during this normally difficult period. The student will benefit from the job experience gained as a replacement or substitute worker. Many of the students can and have relieved regular personnel of their more routine duties to free them for projects which they might not ordinarily find time to perform. As an example of this last possibility, several foundries trained students in plant sand control and sand testing procedure to free the regular laboratory technicians for long-delayed sand and binder research.

Many foundries which do not anticipate immediate additions to their staffs have offered summer placement opportunities. They are to be commended for providing student engineers with the opportunity to increase their knowledge of foundry operations. The work experience that the men will take back to their schools and homes will create a wider interest in the

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utilization of castings. The work experience is only one of a large number of advantages which the students derive from summer foundry work.

Benefits Afforded to Students

The student, first of all, can discover if he is suited to work in the foundry field. He has the opportunity to observe the foundry and has a good idea of his chances of personal success in the foundry field as a whole or in the particular foundry in which he worked as a summer student.

Summer work offers an opportunity to observe the practical application of academic principles learned in school. The student can observe much of the art and applied science of foundry practice which cannot readily be included in college level curricula and has an opportunity to discover inadequacies in his preparation for a foundry career. By direct contact he can gain an understanding of the workers, their likes and dislikes, the workers' reactions to various types of management and the workers' relationship to efficient production. In addition, the student will have the opportunity to observe successful management in practice and some of the consequences of unsuccessful management practices.

Lastly, there is the fact that summer work is a medium through which the student can make lasting contacts in the metal casting field.

Work Survey Participants

The summer workers polled were primarily Junior and Senior students enrolled in metallurgical, mechanical, industrial and general engineering courses and business and engineering administration from 15 engineering schools. The largest number came from cooperating Foundry Educational Foundation schools. They worked in foundries sand casting all the common alloys, employing from six men to about ten thousand men, and pouring from half a ton to two thousand tons per day.

The average student had one or more college-level foundry courses. Approximately one-half of the students had worked in a foundry the previous summer and ten per cent had foundry or related experience ranging from four months to six years prior to entering college.

Types of Jobs Assigned to Students

The cutback in production just prior to and during the past summer resulted in a decrease in summer placement opportunities and a restriction in the type of job assignment. However, the author believes that the reactions of the students to the jobs, their comments and reactions to the questions are typical of any summer work carried out to familiarize the individuals with the foundry industry.

Figure 1 shows how the services of the student workers were divided among the various foundry departments. The graph has been generalized since majority of the students were shifted between two or more departments. Of the men polled, 45 per cent served in one department, 20 per cent in two, and the balance in three or more foundry departments.

One of the surprising facts revealed was the lack

of job contact in the pattern shop. Several men reported short work assignments in pattern storage and distribution. Many of the men undoubtedly used their free time to acquaint themselves with the pattern shop and patternmaking procedure, but will certainly lack in the knowledge which would follow with daily work in this important phase of foundry operation.

The students apparently were well adapted for foundry laboratory work. Many foundries used students for sand testing and control, sand and binder research, in physical testing or for chemical analysis work during a portion of the summer. Some of the men with previous foundry training were assigned to yield control, quality control and casting defect studies.

In the coremaking department few men had the opportunity to make cores. Labor assignments were more common. Loading and unloading core ovens, shoveling sand, handling core sand ingredients, core cleaning, dipping, drying, pasting, assembling, repairing, distribution of cores to molding stations, miscellaneous labor and general clean-up work made up the bulk of coreroom jobs reported in the poll.

A large proportion of the students worked on molding jobs as machine operators or helpers and on floor and pit molding. Other jobs were: sand system labor, muller operator, mold bed and box maker, pouring, shifting weights, shakeout, sorting of hot castings.

In the melting department cupola charging, charge makeup, cupola and ladle repair, drop cleanup, chill taking, tending cupola, temperature reading, alloying and metal distribution were the iron foundry jobs. In nonferrous and steel foundries students worked as melter's helpers.

Visual inspection, magnafluxing, straightening, sorting castings, distribution of castings from conveyor lines, loading mills, grinding and inventory were typical cleaning room assignments.

The men who worked on maintenance jobs gained invaluable experience in installation, repair and adjustment of coremaking, molding, cleaning equipment, sand system and materials handling equipment.

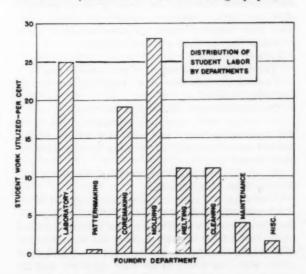


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Students who had a more extensive background in foundry and related work received assignments in production methods, development of new core sand mixtures, test engineering and experimentation and minor supervision.

Several men were employed as "foremen helpers" or "training foremen" and assigned to a foreman in each foundry department for a one-week period throughout the summer.

Did Assignments Allow Time to Observe?

When the students were asked, "Did your job assignments allow sufficient time to observe general foundry operation as much as might be desired?" typical answers were:

"No; assigned a definite job, did use after work hours to observe."

"Restricted pretty much by our range of vision."

A man assigned to blowing excess core wash off newly dipped cores: "The men who dipped cores were on piecework. I had little opportunity to observe."

"In this small foundry I was able to observe all operations performed from day to day."

"My work brought me in contact with all departments."

Forty-seven students reported ample opportunity to observe the overall foundry operation. Observation was more complete where a rotation training was used or where men were assigned a job which carried them throughout the plant. Many men stated that their duties restricted them to small areas but that they took advantage of rest and lunch periods and before and after work hours to observe other operations as much as desired.

Degree of Physical Effort Required

There is always a possibility that a prolonged work assignment, which is excessive for the student's physical condition, might discourage his further interest in the foundry industry. The following question was included in the questionnaire: "Was the physical effort required excessive for your physical ability?" effort was required only occasionally, according to 16. Two men reported work assignments which were definitely beyond their physical capacity.

Typical comments:

"The job improved my physique considerably."

"Considerable physical effort required, but it was not objectionable."

"No physical effort required at any time."

"A 'college man' must prove himself equal to, if not superior to every and any task before wholehearted acceptance and cooperation on the workers' part occurs."

"If you do not like the working conditions the foundry is no place for you."

"The work was hard at times, but I enjoyed it."

After the first weeks of foundry work, when muscles, more accustomed to sliderules, had become adapted to the tools of the foundry most of the students were able to take the work in stride. Occasional periods of excess heat and heavy labor were expected and accepted as a regular part of foundry work. Proper

performance on all types of work aided in obtaining full cooperation of foundry staff and workers. The student found that a sense of satisfaction is gained in proving oneself capable of performing a difficult job.

Contact with Workers and Unions

With rare exception, the students found the workers with whom they had contact on their jobs cooperative and willing to help, after the students had proved themselves to be "regular guys." Typical answers to the question "How did you get along with the workers?" were as follows:

"The men were very agreeable. I enjoyed working with them."

"I found the men friendly and willing to help."

"Some resistance to acceptance because of a recent lay-off in the foundry."

"A college man must show himself to be 'regular'." Either from prior work experience or as a result of experiences on the first days on the job, the students found that "to work hard, keep their eyes and ears open and their mouths closed except to ask questions" resulted in the greatest amount of cooperation with regular foundry workers. That advice was offered by many of the students to future summer workers. The normal resistance of the foundry worker to new help was undoubtedly accentuated by the production cutbacks affecting most of the foundries.

Many of the foundries had previous understandings with their plant labor organizations regarding the type of job the student worker would be allowed to perform. Several men were required to join the union since their status was that of a foundry laborer in the plant. In other foundries closed shop requirements were waived because of the special aims of the student. Most students were restricted to other than production jobs. The resulting labor assignments provided an advantage in that they allowed the student a better opportunity to observe more than one operation or process. However, only by actual performance can many of the important job details be absorbed.

Some of the answers to a question concerning the students' contact with the union follow:

"The workers asked us to join the union, but after they found out we were only going to be there for the summer they did *not approach* us on that subject again."

"I did not have any contact with the union. Students placed on salary or 'engineer-in-training' status were not required to join the union."

Cooperation of Management, Personnel and Training Offices

Although it would be entirely possible for a student to find a summer foundry job and get along without special recognition from the foundry staff, for best stimulation of engineering interest the cooperation of management is essential. The student can learn by doing his assigned tasks, and by seeing other operations, but to get at the hidden problems and detail he must learn through the answers to his questions. Generally there is only a limited amount of information that can be learned from the workers.

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The following answers to the question "Did you feel you received sufficient cooperation from top management; intermediate supervision; personnel and training office?" show the manner in which the management and staff received the student workers.

The management took a personal interest in the summer employees. If we had questions, all we had to do was ask the foremen, superintendents, or men from the training office. If necessary, they would take us out to the portion of the plant in question and point out the answers. It was by far the most complete coverage of questions that I have had the privilege of going through. The directors of the foundry training program were exceptionally helpful."

"We were made to feel free to bring out any problems or suggestions we might have in connection with our stay at the plant. A very good gesture on their part was to organize classes for the group of summer students. Classes were held several hours a week consisting of talks by specialists from various departments and tours through plants connected to the foundry. Although the classes were on our own time, the company did more than their part in providing the additional opportunity to learn more about the

"The cooperation of everyone in the plant was excellent. No question was too insignificant for them to answer."

"The immediate supervision was as cooperative as could be expected for, besides their responsibility in turning out production, they had to keep their regular help satisfied."

"Immediate supervisor excellent. I was given no opportunity to move to another job or introduced to an engineer or anyone in top management."

The answers of about 95 per cent of students polled expressed complete satisfaction with the manner with which management received them. The additional opportunity provided by many foundries to discuss their operations in special meetings or classes was beyond expectations and greatly appreciated. The remainder realized that all levels of management gave as much aid as their duties would allow.

Recommendations to Future Summer Workers

The attitudes of the student workers in reflecting on their experiences this past summer are well demonstrated by the suggestions they made to future summer workers in the foundry industry:

A. About the monetary return:

"The student should take job not merely to earn money, but to learn as much as possible."

"Don't press the matter of wages. After all, you are there to learn and the company will pay fairly.'

'Students should place less emphasis on the monetary return. Be willing to work for less in order to gain experience."

B. About the proper work attitude:

"Pay attention to the working rules of the company; these include safety and being on time."

"Anyone going into a foundry for the training should be willing to do anything, keep his eyes and ears open and his mouth shut."

"Pitch in. Be a part of it and do not be afraid of dirt or work and you will get more from summer

C. To get along with foundrymen:

"Get acquainted with your fellow workers. Ask them questions about the foundry operations but never try to give advice or tell how you learned to do it in school.

"Use more than a little diplomacy in getting along with the foremen."

"Keep your eyes and ears open and mouth closed when there is operational trouble; ask questions when it is all over."

D. To learn more from the work:

"If a student keeps his eyes and ears open and asks questions tactfully he can gain much from his summer job."

"One must gain each individual worker's confidence before the 'tricks of the trade' are forthcoming.'

"See more than what goes on in your particular job or department."

"Take notes on anything that puzzles you. Talk them over with the foreman when he is not busy."

E. Other suggestions:

"Try to get the management to make a statement so that an understanding will be reached at the start concerning the type of work the student should expect. Even the most menial labor is not so bad if it is expected."

"Try to work in a medium to small-sized production foundry and to have as many jobs as possible."

'Try to locate in the type of foundry operation you desire to go into permanently.'

"Have an understanding with the company that you

will be moved throughout the plant.'

"Get a job in a small foundry first. The big production foundry is more or less complicated and the overall picture hard to grasp."

Summer Work Programs

Many foundries can provide only a limited amount of attention to student workers. It is not necessary that a summer work program be unduly complicated by a detailed work schedule, by organized classes or other practices which would result in loss of production time on the part of management. In any foundry where the student receives the feeling that he is wanted and where he has the opportunity to gain practical experience, he will consider the summer well spent. The following are some suggestions, offered by the students, which might aid foundries participating in future work programs.

If at all possible, make work assignment plans for the summer known to the student at the start of his employment. The most difficult jobs are not so hard to perform when the work is expected. Attempt to place the students who are assigned to a single job for the duration of the summer where they will have an opportunity to observe other foundry operations. Also, it may be advantageous to the foundry to hold short discussion sessions, with a supervisor presiding, especially where a group of students are employed. The students can note the things that may puzzle them on the job and find the answers outside of production hours in these discussions. One foundry invited the students to sit in on classes conducted for engineer trainees and apprentices, others conducted classes and tours for student workers.

Most of the students were in favor of a summer work program which would provide job experience in at least two of the basic foundry departments.

Summary of Results

All of the student workers who participated in the poll were satisfied with the results of their summer's work. The summer work poll was conducted to obtain students' analyses of the programs offered by the various foundries. A few of the students were extremely critical, the majority however found opportunity and interest on their behalf which was beyond their expectations. They felt that the organizations had provided an excellent opportunity to learn as much about foundry practice and control as was pos-

sible during the summer period.

Some reactions as to the results of the summer's work:

"I learned more this summer than I could have possibly hoped for. The job was an excellent opportunity to correlate the practical side of foundry with the technical as taught at"

"For my part the foundry has earned my interest and contributed greatly to my engineering background."

As one of the students commented:

"Just as one 'gets out of something what he puts in' one's work will be as enjoyable and pleasant as he wants to make it. There are no jobs too menial for an individual just because he's had some courses in college." The young man looks upon the menial job as an introduction to growth as an engineer or supervisor of other men in the foundry.

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FOUNDRY CLEANING ROOM ABRASIVE OPERATIONS

By

Boyd H. Work*

FOUNDRY CLEANING ROOM GRINDING OPERATIONS involve in general only those machines and abrasive wheels used in rough grinding or so-called snagging operations.

Snagging or rough grinding operations consist of removal of excess or unwanted material from a piece or product through the use of manual grinding operations, for example, the removal of sprues and gates on castings.

The rough grinding operations are differentiated from precision grinding operations by the fact that close tolerances and high finishes are not usually involved. The removal of stock or metal becomes the paramount function of the grinding equipment and the abrasive wheel.

In precision grinding operations, the conditions surrounding the operation are fixed. The work pieces are well standardized, particularly in mass produced items, or are limited in size and weight by adequate predetermined machine size and capacity. Stock removal is almost a constant from piece to piece, as is the distribution of stock on the work piece. Machine and work movements are mechanically controlled, with the result that work production, power consumption, work-wheel contact time, operator fatigue, and practically all other cost factors become predictable elements in the calculation of unit grinding costs.

In contrast, in snagging operations, particularly in the average foundry, work pieces are in almost infinite variety with regard to shape, size, weight, and particularly stock removal. Stock removal even on identical pieces varies, and distribution of the material to be removed is nonuniform and affects the stock removal rate of the grinding wheel to the extent that the time cycle cannot be predicted for equal overall metal removal.

Machine and work movements are manually controlled. Operator ability and judgment, rather than mechanized movements, determine work production, power consumption, work-wheel contact time and wheel cost. There are, in addition, two less tangible factors, namely machine maintenance and operator

fatigue. Practically all cost factors become relatively unpredictable. The overall time cycle of any one operation is influenced further by the non-uniformity of distribution of the stock to be removed, and adds to the difficulty in calculating and comparing unit grinding costs.

What can be derived from these contrasting conditions? Are the variables so multitudinous that nothing can be done about them? Is there a solution?

There is a solution, but not through the use of any standardized or universal formula.

Much can and has been done to reduce foundry cleaning room cost. Much more can be accomplished through a thorough understanding of the factors effecting these costs.

Let us then proceed to restate these factors and correlate as many as time will permit.

Machine Equipment in Foundry

Rough grinding operations in the foundry are performed on three basic classes of machines:

- Swing frame grinders
 Floor stand grinders
- 3. Portable grinders (air or electric)

In addition, many other specialized machines are in general use, such as cut-off machines, backstand grinders using coated abrasive belts, disc grinders for surfacing, etc. In the manganese foundries we also find surface grinding machines such as side surface grinders, horizontal spindle and vertical spindle surface grinders, and large cylindrical and internal machines. In the pipe, sanitary and hollowware foundries, we also encounter various other types of specialized grinding machines. This discussion will, however, be confined to the three basic types in general use.

All of these three basic types of equipment are designed to operate in two distinct speed ranges. The speed determines the type of abrasive wheels that can be used. The so-called low speed machines utilize vitrified abrasive wheels which can be operated at speeds not to exceed 6500 sfpm; the high speed machines utilize resinoid or rubber bonded abrasive wheels which can be operated at a maximum of 9500 sfpm. The factors influencing choice of machines,

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speeds and abrasive products will be discussed more fully under each machine classification. Higher stock removals per contact hour can be obtained, under proper conditions, with the high speed wheels of the resinoid and rubber types of abrasive wheels.

Swing Frame Grinder

These machines are manufactured in a variety of sizes carrying wheels in diameters from 12 in. to 24 in.

The old type swing frame grinders were of L-type construction, driven from an overhead shaft and suspended from the top of the L. The vertical and horizontal members were hinged at the points of suspension and intersection of the members to allow vertical and horizontal movement of the wheel, with the wheel spindle located at the open end of the horizontal member. The wheel spindle was driven by two belts, one vertical from the main shaft to and through a countershaft at the intersection and from this countershaft through a horizontal belt to the wheel spindle. This type of machine is practically obsolete and has been replaced by an individually powered unit which consists of a horizontal frame supported by a pedestal or suspended by a centrally located cable or chain with a motor at one end and wheel spindle at the opposite end. Power is transmitted through a multiple V-belt drive direct from the motor shaft to the wheel spindle.

This new type of construction allows a greater range of machine movement, as the upper point of support may be of the movable type travelling on a rail or hung from a combination rail or jib crane. For rapid wheel height adjustment, a chain hoist may be introduced as the central suspension member.

The new type of machine has the following advantages:

- 1. It is adapted to higher spindle speeds.
- 2. It can cover greater working area.
- 3. It is easier for the operator to manipulate, and can be balanced more readily.
- 4. There is less down time. Being individually powered, only a single machine need be shut down for wheel changes, repairs or delays due to crane service.
- 5. Wheels can be mounted on the wheel spindle between bearings for high speed (9500 sfpm) operations.
- 6. There is more positive drive through the multiple V-belt.
 - 7. It allows more angular swing to the wheel head.
- 8. Speed changes are provided to compensate for wheel wear.
- 9. There are also many other advantages in safety, guarding, belt maintenance, etc.

Application and Field of Use

The swing frame grinder is used for those applications where the work being ground cannot be presented to the wheel face manually. Through and within the mobile limits of the machine, the wheel is taken to the work.

The usual procedure is to deliver the work to be ground to the working area of the machine and through machine manipulation the various areas are ground as required. Heavy work may require additional crane service to expose new areas. This is mentioned as lack of crane service may be a large factor

in delays, materially and adversely affecting grinding costs.

Machine Size

The larger and heavier the machine, the greater the operator fatigue. The large machines carrying 24 in. x 3 in. wheels are satisfactory on heavy castings, particularly steel castings where large volumes of metal are located in localized areas and the essential machine movements are relatively limited.

More power and wider wheel-work grinding contact can be provided, giving higher stock removal rates per contact hour.

Where metal is distributed over large areas and relatively little stock is to be removed, rapidity of machine movement is essential to rapid stock removal and lighter machines are indicated to lessen operator fatigue. The medium weight machines carrying wheels 18-in. to 20-in. diam and 2½-in. to 3-in. wide allow easier manipulation of the machine, where the cleaning of broad surfaces is the major problem and the localized areas of burned-off gates and risers are less in volume than in the case of the heavier castings.

Horsepower

Adequate power should be available with adequate transmission from the motor to the wheel spindle, as inadequate power results in high electric motor maintenance, lowered stock removal rates, higher abrasive costs, and higher drive (V-belt) costs.

Relative to the subject of power, it should be stated that machines of the swing frame type are generally designed and sold for operation with the machine in balance and to carry relatively light loads on the wheel head. Added weight on the wheel head creates higher working pressures at wheel-work interface or contact.

It is not uncommon to see weights, usually in the form of scrap castings, loaded on the wheel head to such an extent that power and transmission become inadequate to drive the wheel without incurring heavy overloads. This is done to promote higher stock removal. If higher stock removal rates are desired, softer wheels should be used, thereby lessening frictional losses, avoiding higher machine maintenance and overloads and unnecessary operator fatigue.

Sharpening or Dressing Wheel Face

The problem of wheel dressing is not usually a factor of economic consequence on swing frame grinders, due to the fact that the intelligent operator finds sufficient sharp corners or fins on which to dress or resharpen the wheel face. If the wheels used tend to glaze in grinding large, heavy work areas, the immediate operation can be interrupted momentarily and the fins of the casting can be used to resharpen the wheel face. This subject will be covered more fully in the discussion on stand grinders.

Bedding or Casting Support During Grinding

A stable bedding or casting support should be provided for control of the work during the swing frame grinding cycle because vibration of all frequencies and rolling or rocking movements of the casting during grinding are costly from the standpoint of stock re-

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moval rates, abrasive costs, and operator fatigue.

This vibration, in either the machine or work, prevents the operator from maintaining the constant and uniform pressures so essential to the desired maximum uniform stock removal rates. The higher frequencies set up a constant pounding of wheel on work, resulting in higher abrasive consumption and costs. Such operation may result in a reduction in the stock removal rate-as high as 50 per cent.

Where vibration is well dampened, a steady, even, heavy spark flow is obtained, in contrast to the intermittent, heavy and light spark flow where extreme

vibration is present.

Instability of the work support not only lowers the stock removal rate, but complicates the manipulation of the grinder by keeping the operator off balance. In extreme cases, the operation becomes hazardous and at all times is fatiguing and unsatisfactory.

Wheel Speeds

As pointed out previously, swing frame grinders are designed to operate in two distinct speed ranges and the optimum safe operating wheel speeds must be maintained in order to attain maximum stock removal and resultant lower abrasive costs.

Each machine is designed for the operation of a wheel of specific maximum diameter with spindle speeds designed to operate at a maximum of either

6500 or 9500 sfpm (surface feet per minute). The "low speed" (6500 sfpm) machines are designed to use vitrified or ceramic bonded abrasive wheels. The "high speed" (9500 sfpm) machines utilize the stronger grades of organic bonded wheels. These wheels may be bonded with synthetic resins described as resinoid wheels or bonded with rubber and identified as such.

Resin and rubber bonded wheels may, and are, sometimes used on the low speed machines operating at speeds of 6500 sfpm. Vitrified wheels, however, must not be used on "high speed" (9500 sfpm) machines. This subject is thoroughly treated in the "American Standard Safety Code for the Use, Care and Protection of Abrasive Wheels.'

It is obvious that the surface or periphery speed of the grinding wheel is reduced as the diameter of the wheel becomes smaller through wear, if the speed of the wheel spindle, rpm, is maintained constant.

By way of illustration, let us assume that a 24-in. wheel is mounted on a wheel spindle operating in the "high speed" range at 1512 rpm, resulting in a wheel periphery speed of 9500 sfpm.

The table shown below will illustrate the loss of surface speed due to wheel diameter reduction at the

constant speed of 1512 rpm.

1512 rpm 9500 sfpm 24-in. diam 22-in. " 8708 22 20-in. 7917 18-in. 7125 6333

From the table it can be noted that the wheel speed has been reduced to 7917 sfpm at 20 in.

Since most machines are equipped with at least one speed change effected by changing belts to alternate

pulleys, these can be used to bring the spindle speed up to 1814 rpm at 20 in.; restoring the original sfpm of 9500.

Since loss of speed causes any given grade of wheel to act softer and results in increased abrasive loss, it is necessary to maintain speeds at the nearest optimum safe speed in order to obtain the greatest efficiency. For this reason, it is imperative that the speed changes built into your equipment be utilized.

The same result can be obtained by remounting the 24-in. wheel on a 20-in. machine when the diameter has been reduced to 20 in.; as the 20-in. machine will provide for the higher necessary rpm for the main-

tenance of the desired sfpm.

Reasonable abrasive efficiency can be expected with proper wheel grade placement if the resinoid or rubber bonded wheel speeds are maintained between 8000 and 9500 sfpm, and the vitrified wheels in a speed range of 5000 to 6500 sfpm.

The Stand Grinder

These machines carry a wider range of wheel sizes than swing frame grinders, carrying wheels from 12 in. to 54 in. in diameter. The usual range used in normal foundry practice is from 14-in. to 36-in. diameters.

Stand grinders are usually of the double end type, consisting essentially of a wheel spindle mounted on a stand or fixed base with a wheel mounted on each of the overhanging ends of the spindle. However, several models designed for single wheel operation are available, and various types of drives are utilized.

1. The wheel spindle may be driven from an overhead shaft, through a belt drive to some central point of the wheel spindle.

2. The wheel spindle may be an integral part of a centrally located motor mounted in or on the base.

3. The wheel spindle may be driven by a motor set in the base of the machine, through flat or multiple V-belts to the wheel spindle. These models are available with two and three wheel spindle speed changes to allow adjustments for wheel wear.

Application and Field of Use

The stand grinder of single or double end type is used to grind work which can be manually handled and presented to the wheel face. This manual handling of the castings is in contrast to the machine manipulation involved in the swing frame grinding operations.

Fixtures and light chain hoists may be used as auxiliary aids to manual operations, but the work is usually lighter and the grinding is done on more restricted areas than with the swing frames. There is considerable overlapping without loss of economy between swing frame and stand grinding operations. In many cases light work of repetitive nature can be securely clamped or held in a fixture and ground with equal or greater overall economy with a swing frame grinder. Selection of machine type for a given operation cannot be fully determined by casting weight, as illustrated above.

The factors affecting economical operation of stand grinders are as follows:

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Machine Size

Since in the case of stand grinders the machine position is fixed, added size and weight place no limitation on the operation as in the case of swing frame operations. In fact, from most points of view the larger machines are more desirable, productive and even more economical.

Casting Size

Casting shape, size and weight and the handling and movements necessary to present the work to the wheel are the factors chiefly affecting operator fatigue. Since many grinding operations are performed with actual body contact with the casting being ground, any resulting vibration can place severe strain on the operator.

Reinforced leather aprons can be worn by the operator to lessen and distribute these forces and should always be used where direct body contact is long sus-

tained.

Whereas in swing frame grinding wider wheel faces are usually indicated, on stand grinding thinner wheels may be used advantageously to increase unit workwheel pressure, by reducing the width of contact.

Horsepower

Most stand grinders are sold and powered for manual operation, independent of the use of mechanical aids. The added use of levers, pressure bars and other mechanical aids, to increase pressures at the wheelwork interface, create overloads on the motor and drive, and unless adequate power and transmission is provided, high maintenance costs of the motor, power transmission and spindle bearing will result.

In many cases the operator uses the casting or work as a lever, with the work rest as a fulcrum, or he wedges the casting between the work rest and the wheel face, to obtain leverage for resultant increase in grinding pressures. Dropping or lowering the rest to permit grinding below the horizontal center line of the wheel produces the same result. This practice is dangerous for the work is often caught between wheel and work rest, resulting in wheel breakage.

It is not to be inferred from the above that levers and higher than normal working pressures are undesirable. Such is not always the case, but on any grinding operation where working pressures are set up beyond the machine and power limitations, the operation will prove costly from the standpoint of efficiency

of operation and maintenance costs.

It must further be recognized that on double end stands, involving two wheel operations on a single spindle, the power requirements involve two wheels and two operators grinding simultaneously. Due to the single wheel operation on swing frame grinder, the single operator can control overloads. Such is not the case with two wheel operations from a single power source and for this reason, wheel operations are more frequently found to be carrying overloads.

Problems Arising from the Operation of Two Wheels on a Single Spindle—The double end stand grinder carries two wheels on opposite ends of the same wheel spindle driven from a single power source. The safe

operating spindle speed in such cases is, of necessity, determined by the diameter of the larger wheel. If there are extreme diameter differences, the smaller wheel becomes inefficient, resulting in a lower stock removal rate and a higher abrasive loss and a consequent increased abrasive cost. To control and more nearly equalize wheel diameters throughout the life of the two wheels, several stratagems have proven effective. Unequal wheel wear stems from several basic causes, namely—

A. Differences in work types ground at the opposite

ends of the machine.

B. Differences in operator grinding techniques.C. Differences in physical strength and statute of

operators

D. Differences in characteristics of the materials being ground.

To create the optimum of standardization of the grinding operation, the following steps may be taken:

A. Operators should be matched or teamed for each double end stand, according to their techniques, work habits and physical strength.

B. The work to be done on each wheel should be

classified and equalized.

C. Work of similar types with respect to weights, shape and type, stock distribution and metallurgical characteristics should be routed to the two ends of the machine, or if this proves uneconomical, equal amount of each variety should be routed to each end or operator.

The adoption of all of these steps will tend to equalize wheel wear and tend to maintain uniform wheel diameters. The degree and extent to which such classifications are practical depend on the size of the overall operation, but much can be accomplished by this type of classification, even with limited standardi-

zation on even the smallest operation.

In passing, it should be mentioned that the psychological effects of such procedures are excellent. Men like competition, and if the rules are correctly written—the stint, the job, the game is played on a basis of equality—competition becomes the keener. Handicaps due to inequalities generally stifle competition.

Dressing and Truing Abrasive Wheels

Several new phases of this problem are presented by stand grinding operations. Let us first consider the dressing or sharpening phase. Abrasive material dressed off from a wheel by a dressing tool represents two losses, namely, non-productive abrasive and the

loss in productive grinding time.

Dressing and attendant losses can be minimized through proper selection of abrasive wheel specifications. Glazing of the abrasive wheel is generally caused by the fact that the grade of abrasive wheel used is too hard or the grain size is too fine. Correction of abrasive specification will tend to eliminate the necessity for frequent dressing. Excessive dressing, where proper abrasive specifications are in use has been, by general observation, a result of habit rather than necessity.

As pointed out in relation to the same subject discussed under swing frame grinders, the well instructed operator replaces the dressing operation by intelligent B. H. WORK

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handling of the casting, thereby adding to his productive time. In practically all cases observed over past years, the dressing habit once formed by the operator leads further to excessive dressing.

In general, the operator who has formed improper work habits is usually the proverbial square peg in the round hole. Elimination or a continuing policy of education and supervision is essential to proper control and is the only solution to this problem. A symposium could be conducted on this subject.

Secondly, let us consider the truing phase.

True running of the abrasive wheel is more vital to successful operation of stand grinders than to the operation of swing frame grinders, since the resulting vibration is transmitted directly through the work piece to the operator. As in dressing, the operator can influence the truing cycle by proper manipulation of the casting. Added truing operations are encountered where the operator through negligence does not or through limitation cannot traverse the work.

Where the stock to be removed is located in such a manner that the casting movements are limited to direct infeed (perpendicular to the wheel face without transversing movement) the wheel may become grooved. If the same operation is continued for long, the high shoulders that are built up must be reduced by dressing.

The width of the grinding wheel in use in relation to the grooved width will determine the amount of abrasive loss incurred in the subsequent truing operation. The use of a narrower wheel may curtail or eliminate such losses.

Grinding on the corners of the wheel likewise develops angular wheel faces and similar attendant losses, and should be avoided wherever possible.

All grinding should be done if possible across the entire wheel face, or if the contact is too narrow, the work piece should be traversed. The use of levers, so-called pressure bars, tends to eliminate proper traversing movements.

Portable Grinder (Air or Electric)

Portable grinders are manufactured in a variety of sizes carrying wheels, from ½-in. to 14-in. diameter. The portable, as the name implies, is a light tool combining a power unit, wheel spindle (usually integral), and housing, all of which is manually operated and usually without mechanical suspension or support. Special heavy duty machines especially designed for foundry uses are available in either pneumatic or electric type.

The most popular machines carry Type 1 (straight wheels) 3, 4, 5, 6 and 8 in. in diameter. Plugs and cone shaped wheel Types 16, 17, 18 and 19 may be used on standard models or on lighter models designed for their specific operations. Machines of the "right angle head" or sander types are designed for the operation of Type 6 and 11 portable cup wheels.

The speed range of these machines follows the pattern of previously described equipments, as the machines in this class are operated in both low speed (6500 sfpm) and high speed (9500 sfpm) ranges.

Application and Field of Use

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The portable grinder is always taken to the work and manually operated. It is used in conjunction with and complements the work of both swing frame and stand grinders, and has a field in its own right.

It performs the lighter grinding operations on the heaviest work pieces where heavy stock removal is not a requirement. It is a necessity in grinding work areas which are inaccessible to either stand or swing frame grinders, such as holes, radii, fillets, etc.

The portable grinder is ideally adapted to operations where light stock removal is indicated, on such operations as light weld reduction, light parting line fins, and on large areas where the removal of surface scale or pimples is involved.

Machine Application and Safety

Diversity of application of this equipment makes it impossible to discuss this field in detail. The principles discussed on the two machine types previously described are generally applicable to portable grinders.

However, several points based on general observations may be worthy of note. Those concerning safety of operation seem to be most important.

Since wheel diameter and arbor holes, are identical on both high and low speed machines, there exists an interchangeability of mounting and a frequent hazard is created by mounting the vitrified, low speed wheels on high speed equipment. Supervision and operators must be instructed and machines identified so that this will not occur. Many plants resort to painting the machines different colors to accomplish this identification.

The machine spindle speed is controlled by governors on most air grinders and periodic inspection should be made to see that these are operative and have not been changed or altered.

The wheel guards provided with the machines should be used. Where this is impractical, special studies should be made to provide for the safe operation of the machine and wheel equipment. On other machine types, such as swing frame and stand grinders, adequate guards can be provided to contain the wheel fragments in case of breakage.

The problem with portable wheel operations is more difficult because of the unusual flexibility of the operation. Wheel exposure with proper guards and machine manipulation may be adequate for operator protection, but will rarely protect the area surrounding the operation. Many plants now provide portable screens or completely enclose the work, machine and operator in grinding booths.

General Discussion of Abrasive Costs

Grinding costs are made up of three elements:

- 1. Abrasive cost (wheel cost)
- 2. Labor cost (labor hours, piece rates, etc.)
- 3. Overhead (machine costs, power maintenance, etc.)

A proper balance must be established among these factors to give minimum overall costs. Operational factors influencing these cost elements have been pointed out under each machine heading to enable you to analyze your specific problems. No detailed cost analysis will be attempted here.

In passing, it might be well to point out several obvious influencing factors and to stress several other related factors which have been neglected.

Hard wheels give lower removal rates, and tend to increase labor and overhead costs and to lower abrasive costs. Soft wheels give higher stock removal rates, and tend to increase abrasive costs and to lower labor and overhead costs. Higher working or grinding pressures tend to increase stock removal rates, thus decreasing labor costs, but increasing abrasive costs. These facts are more or less obvious.

The statement has been made previously that "high speed" wheels cost more per cubic inch of usable abrasive, they must show reduced labor and overhead costs to be economical.

The greatest economies are shown, therefore, where actual grinding or contact time is highest, and where handling time or interruptions for crane service are a low percentage of the overall time. As an example, let us assume that a wheel will remove 40 lb of stock per hr at a maximum uniform cutting rate, and that under identical conditions another wheel will remove 20 lb per hour. If the contact time is 100 per cent, the 40-lb per hour wheel will remove 40 lb of stock per hour and the 20-lb wheel will remove 20 lb. In our first assumption, metal is presented to the wheel face so that it cuts at a uniform optimum rate for the full contact time. This is rarely the case, due to stock distribution.

Let us make one other assumption to illustrate the point that there is a wide difference between grinding contact time and optimum effective grinding contact time.

We will assume the same optimum stock removal rates as stated in the first condition, but will assume further than although both wheels are in continuous contact, "throwing sparks," the rate at which metal is fed to the wheel is varied. In an hour's contact time, the 40-lb per hour wheel is fed for 50 per cent of the contact time at its optimum rate and will remove 20 lb of metal. The remaining 50 per cent of the contact time, it is fed stock not at its optimum rating, but at the rate of only 20 lb per hour, and will remove 10 lb. Overall stock removal will be 30 lb.

The 20-lb per hour wheel, in an hour's contact time, is fed for 50 per cent of its contact time at its optimum stock removal rate of 20 lb and will remove 10 lb of stock. When fed at the same rate as the 40-lb per hour wheel (which is the optimum rate of the 20-lb per hour wheel), it will remove another 10 lb of stock. Overall stock removal will be 20 lb.

In the first case, the 40-lb per hour wheel removes 20 lb more stock than the 20-lb per hour wheel, an increased removal of 100 per cent.

In the second case, the differential is only 10 lb per hour or 50 per cent increase in stock removal.

The 40-lb per hour wheel has lost 50 per cent of its advantage or effectiveness due to the fact that it was not cutting for the full time at its optimum removal rate.

The distribution of stock on a given piece to be ground determines to a great extent what percentage

of the time a wheel is cutting at its optimum removal rate. The percentage of contact time in relation to overall time is therefore a deciding factor in determining whether "high speed" wheels which give high stock removal rates will prove economical, and determine to a large degree the extent of the resulting economy.

This probably accounts for the fact that throughout the steel foundries, "high speed" high production wheels show unquestionable economy, while in some malleable foundries the use of resinoid wheels, as opposed to vitrified, is a moot question; and in the gray iron foundries, vitrified wheels still predominate.

Steel castings in general present large volumes of metal in more restricted areas, resulting in a greater percentage of contact time at the optimum cutting rate. Heavy malleable may approach the conditions found in steel castings, but with gray iron castings, parting line fins and the light or clean, closely broken gates do not allow the wheel of high production rate to be operated at or near its highest efficiency.

The grinding of hard or white iron unannealed malleables presents a problem of a similar nature to the gray iron insofar as stock distribution is concerned, but the questionable application of "high speed" wheels stems more from the inherent hardness of the materials themselves than from any other single factor. In the grinding of hard materials in general, low speeds are indicated; better penetration is obtained at slow speed. This is one man's answer to the moot question of the application of "high speed" wheels to these materials.

The most economical wheel size should be purchased. First cost is not all-important, but there are definite advantages to be obtained, due to variation in the cost of usable abrasive in different wheel diameter and arbor sizes. The following table is supplied to direct your attention to this cost factor. Since the wheel stub is unusable material, this table indicates the practical limit of use and gives the approximate cost of the usable abrasive in terms of cost per cubic inch.

Other economies can be effected through quantity purchases. This list is based on *less than unit* purchasing quantities and may not be your costs. Acquaint yourself with all the facts.

One question may arise on examining these tables: why are not large arbors or wheel holes shown in the aluminum oxide vitrified listing? The reason is that such wheels are unsafe and therefore not recommended for heavy snagging operations.

of

For economy's sake, use all the usable abrasive; watch stub discard size. Much profit is thrown away in the form of oversize wheel stubs.

Summary and Conclusions

In summary, cost reductions can be obtained by the following:

1. Through the use of the correct type and size of grinding equipment with ample power to meet the particular needs of your own operation.

2. Through standardization and classification of

work.

3. Through proper selection of abrasive specifications in relation to work classification and equipment. ONS

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ANALYSIS OF COST PER CUBIC INCH OF USABLE ABRASIVE

ALUMINUM OXIDE RESINOID SNAGGING WHEELS FOR "HIGH SPEED" EQUIPMENT

| Wheel Size | Unit List Price | | Usable Abrasive L. Cu In. | | *Adjusted Cost Per Cu In. |
|----------------|-----------------------|----|---------------------------------|---------|---------------------------------|
| 16 x 2 x 6 | \$ 31.80 | 8 | 301.57 | \$.1054 | \$.0991 |
| 18 x 2 x 6 | 41.80 | 8 | 408.40 | .1024 | .0962 |
| 20 x 2 x 6 | 52.30 | 8 | 527.78 | .0991 | .0931 |
| 30 x 2 x 12 | 67.90 | 15 | 551.40 | .1231 | .1158 |
| 30 x 2 x 12 | 111.90 | 15 | 1060.30 | .1055 | .0992 |
| 36 x 2 x 12 | 179.90 | 15 | 1682.34 | .1069 | .1005 |
| 16 x 21/2 x 6 | 38.45 | 8 | 376.98 | .1020 | .0959 |
| 18 x 21/2 x 6 | 50.45 | 8 | 510.50 | .0988 | .0929 |
| 20 x 21/2 x 6 | 62.95 | 8 | 659.73 | .0954 | .0897 |
| 24 x 21/2 x 12 | 81.60 | 15 | 689.20 | .1184 | .1113 |
| 30 x 21/2 x 12 | 133.60 | 15 | 1325.38 | .1008 | .0948 |
| 36 x 21/2 x 12 | 213.60 | 15 | 2102.93 | .1016 | .0955 |
| 16 x 3 x 6 | 44.95 | 8 | 452.37 | .0994 | .0934 |
| 18 x 3 x 6 | 58.95 | 8 | 612.60 | .0962 | .0905 |
| 20 x 3 x 6 | 73.45 | 8 | 791.67 | .0928 | .0872 |
| 24 x 3 x 12 | 95.35 | 15 | 827.04 | .1153 | .1084 |
| 30 x 3 x 12 | 155.35 | 15 | 1590.45 | .0977 | .0918 |
| 36 x 3 x 12 | 247.35 | 15 | 2523.51 | .0980 | .0921 |

* Adjusted for Product Discount

 Through an increased percentage of grinding time. Avoid crane service delay and facilitate work handling.

5. Through the education of supervision and operators on the factors affecting costs and savings.

6. Through giving the same close study and attention to the cleaning room as is given to the melt or pattern shops or the foundry.

In conclusion, two questions arise which do not fall within the scope of this discussion, but they seem pertinent to it and have a definite bearing on the foundry cleaning room costs. The questions are these:

1. How much attention is being given to the original molding practice to the location and amount of stock that must eventually be removed in the cleaning room?

2. How much attention has been given to the reduction of stock to be removed by grinding through closer torch burning or cropping of gates, heads and risers?

As a final observation, it is usually cheaper to leave unnecessary stock off of any work piece than to take it off later.

ANALYSIS OF COST PER CUBIC INCH OF USABLE ABRASIVE

ALUMINUM OXIDE VITRIFIED SNAGGING WHEELS FOR "LOW-SPEED" EQUIPMENT

| | Unit | | Usable | List Cost | **Adjusted |
|---------------|---------|----------|----------|-----------|------------|
| | List | Stub | Abrasive | Per | Cost Per |
| Wheel Size | Price | Diam, in | . Cu In. | Cu In. | Cu In. |
| 16 x 2 x 2 | \$35.20 | 8 | 301.58 | \$.1167 | \$.0805 |
| 18 x 2 x 2 | 44.10 | 8 | 408.40 | .1080 | .0745 |
| 20 x 2 x 2 | 54.15 | 8 | 527.78 | .1026 | .0708 |
| 24 x 2 x 2 | 78.10 | 8 | 804.24 | .0971 | .0670 |
| 30 x 2 x 2 | 124.60 | 8 | 1313.18 | .0949 | .0655 |
| 36 x 2 x 2 | 184.25 | 8 | 1935.22 | .0952 | .0657 |
| 16 x 21/2 x 2 | 42.75 | 8 | 376.98 | .1134 | .0783 |
| 18 x 21/2 x 2 | 53.55 | 8 | 510.50 | .1049 | .0724 |
| 20 x 21/2 x 2 | 65.70 | 8 | 659.73 | .0996 | .0687 |
| 24 x 21/2 x 2 | 95.15 | 8 | 1005.30 | .0947 | .0653 |
| 30 x 21/2 x 2 | 151.75 | 8 | 1641.48 | .0925 | .0638 |
| 36 x 21/2 x 2 | 223.50 | 8 | 2419.03 | .0924 | .0638 |
| 16 x 3 x 2 | 50.30 | 8 | 452.37 | .1112 | .0767 |
| 18 x 3 x 2 | 63.05 | 8 | 612.60 | .1029 | .0710 |
| 20 x 3 x 2 | 77.25 | 8 | 791.67 | .0976 | .0673 |
| 24 x 3 x 2 | 112.25 | 8 | 1206.36 | .0931 | .0642 |
| 30 x 3 x 2 | 178.90 | 8 | 1969.77 | 0.908 | .0627 |
| 36 x 3 x 2 | 262.70 | 8 | 2902.83 | .0905 | .0625 |

** Adjusted for Product Discount

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OUTLINE OF INSPECTION FOR PEARLITIC MALLEABLE CASTINGS

By D. T. Martin*

Pointing to Inspection

Many manufacturers, in their effort to reduce production costs, have readily accepted pearlitic malleable castings to replace more expensive materials.

Also, in the designing of new parts, engineers and process men have been quick to take advantage of the versatile properties found in this cast ferrous metal.

With each new field of application, new casting problems are encountered. Therefore, it is necessary to improve in all phases of foundry operation and control.

Acceptance of pearlitic malleable castings is based on quality. The maintaining of quality will depend upon adequate inspection.

Organizational Structure

Pearlitic malleable castings face a competitive market with other rough materials; therefore, it is essential to use the most economical, but effective, methods of casting inspection.

Usually, a Chief Inspector heads up a foundry inspection department assisted by enough properly trained personnel to supervise the plant inspection duties and maintain frequent contacts with the customers.

Relative to the departmental function, it is sound operational practice to have only enough members of the inspection department to keep a routine viligance on quality through all operations.

In this article, we shall refer to employes of the inspection department as floor inspectors. Workers that examine each casting individually we shall term production inspectors.

The floor inspectors are assigned to the key operating areas in the plant—the foundry proper, the grinding department and the finishing department; or whenever necessary, at any other point that may require special attention.

A floor inspector in an operating area has the responsibility of maintaining quality and aiding in the

prevention of scrap. His objective is to check frequently the quality of the work from the operations, and re-examine the castings that have been inspected and passed by the production inspectors to be sure the castings meet the quality standards. All quality standards are established and controlled by the Inspection Department. The standard of acceptance is usually determined through frequent contacts with the customer or past experience of practice.

Inspection that requires the individual examination of castings should be combined with a production operation that also requires individual handling. By combining operations in this manner, many costly man-hours are saved because most of the secondary handling is eliminated. The production operation having a time standard to meet, also sets a pace for the inspection operation.

For example, on a press straightening operation in the finishing department, the inspector assists the press operator by removing the pressed castings from the die and inspecting it, while the operator is securing and placing another casting into the press. Only a portion of the inspector's time is chargeable to the actual inspection operation, and the remaining time is applied to the production operation. The press operation, having a standard time to meet also sets the pace for the inspection operator.

The supervisor in charge of the production operation is responsible for the performance and efficiency of the production inspector, but the quality standards used are controlled by the inspection department.

Coordination of Customer and Foundry Inspection

It is essential for the foundry to be completely familiar with the machining procedure and end use of the finish part at the customer, if satisfactory castings are to be produced. Also, familiarizing the customer with the foundry's casting problems will help solve the problems through suggestions and counter-suggestions. This puts the foundry in position to establish practical quality standards that will be satisfactory to both the foundry and the customer.

Problems of producing and fabricating pearlitic malleable iron differ in many respects from those of regular malleable, because of more diversified applicati

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Chief Inspector, Saginaw Malleable Iron Plant, Central Foundry Div., General Motors Corp., Saginaw, Mich.

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tions. Some end uses will be more severe and consequently, will require an improved grade of castings.

The inspection representative must not only be well experienced in the field of foundry operation, but also fully acquainted with all fabricating methods at the customer, and the quality requirements of the finished product.

After the layout of samples on a new casting has been approved for production, it is best that a "pilot" lot be machined by the customer before going into full scale production. By closely observing this run, many difficulties may be overcome that will benefit both customer and foundry.

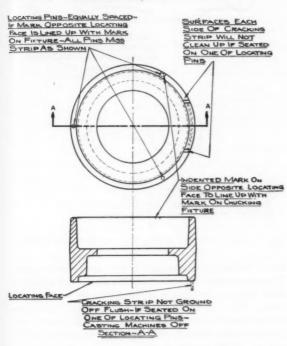


Fig. 1-Sketch of unit drum.

For example, during a "pilot" run of unit drums (see Fig. 1), used in an automatic transmission, a number of castings were lost because a small area on one edge of the outer diameter did not clean up on the turning operation. When the trouble was analyzed, it was discovered that on this edge, which was a locating surface for machining, the foundry required a cracking strip about 2 in. long. This strip had been ground, but not quite flush with the casting's edge. At the foundry, it was realized that with a snag grind, they could not remove the cracking strip on every piece and hold the grind exactly flush with the casting's edge. Therefore, some of the strip had been left to make certain they did not take off any of the finishing stock. Apparently, on the castings that did not clean up, the remainder of the cracking strip had been seated on one of the three locating pins and the piece had been machined off because it was not down to proper location. The difficult part of the problem was that the cracking strip and locating pins were not visible to the machine operator when the casting was placed into the chucking fixture and he was unable to

see if the locating pins were seated on the cracking strip. Of course, the machine shop immediately requested that the excess metal be ground flush with the casting.

The foundry inspection representative then explained that on a snag grinding operation, it was not possible to hold such close tolerances and that many castings would be lost from excessive grinding.

He then suggested casting an indented mark on the edge of the casting opposite the locating face and that also an indicating mark be put on the outer face of the chucking fixture. By aligning the mark on the casting with the mark on the fixture, the locating pins would miss the cracking strip on every piece.

The idea was readily accepted by the customer; hence, the foundry avoided an operation that would cause them considerable difficulty and the machine shop eliminated the loss of machined parts in their future production.

Continued frequent visits at the customer are essential, even though production on a casting is in full swing and most of the problems are under control, for the machine shop may want to revise their set-up or add an operation to overcome a problem at their plant.

The foundry should keep abreast of this information to contribute at every opportunity the correct information on the abilities and limitations of foundry practice. A good example is a planetary gear carrier (see Fig. 2) that had been in production for several

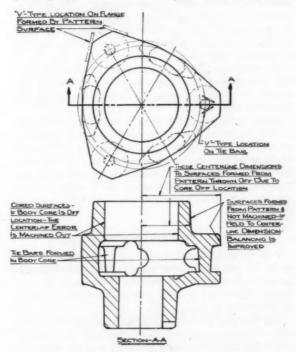


Fig. 2-Sketch of planetary gear carrier.

years when the customer discovered, that in making engineering changes to improve his product, it would be necessary to balance the carrier more closely than he had previously.

At the time the balancing tolerances were tightened

up, the machining set-up located the radial index position with a "V" fixture off a tie bar that held two bearing flanges from excessive warpage at the foundry. The carrier had three sets of bearing flanges, equally spaced around its outer diameter, with the inner surfaces of the casting made from a dry sand body core. The tie bar between the flanges were also made from this core.

It was found that the off-balance condition was caused by a small variation in the core location. The index location, being determined from the tie bar, moved all the error to the outer surface of the casting that was formed from pattern surface. This surface was not machined and so there was no dimensional equalization from the machined center line to the

outer casting face.

The customer requested the foundry to hold the core location to a tolerance that was impractical for sand casting practice. It was explained to the customer that it was impractical to expect to hold the core location as closely as they had requested. If they would find a locating point on the casting that was formed from the pattern surface and on an area not machined, the finished part would balance more closely. The theory here was—all the inside cored surfaces were machined. If the location was made from a surface formed from the pattern, the error from core-off would be machined out.

The customer found they could use a "V" location off the converging edges of one of the flanges formed from the pattern surface and the part balanced satisfactorily. Here again is evidence that familiarity with the customer's operation is vitally important in eliminating or avoiding foundry problems in the making of pearlitic castings or any castings.

If changes are made at the customer that will permit elimination of an operation at the foundry, it should be given immediate attention to reduce the

foundry's cost.

Close dimensional tolerance and other casting limits that do not add quality to the finished product are an unnecessary expense to the customer. Cooperatively accepting castings that serve the end use satisfactorily with the least amount of processing at the foundry, benefits the purchaser. He is using the advantage of obtaining suitable material for his job at the lowest cost.

Inspection in the Foundry Proper

It is recognized in industry that regardless of how good the inspection, the more scrap produced, the greater the amount of scrap that will reach the customer. Therefore, a scrap preventative program must be constantly active.

The foundry department is the first operating point where the common causes of scrap are the greatest, and is one of the key areas in which to place members

of the inspection department.

A properly trained inspector in the foundry department, who readily recognizes scrap caused from improper gating or improper foundry operation, can be a major factor in obtaining the quality required on most pearlitic castings. His duties are to make routine spot examinations for quality as often as possible. The

frequency of these checks is governed by the type of castings being made and the volume produced.

Sequence of examination required on each casting depends upon design, dimensional tolerances, methods of machining and the final application of the finished part.

Making of automotive engine rocker arm castings exemplifies the need of foundry routine inspection (see Fig. 3). The height of the bearing boss is coined

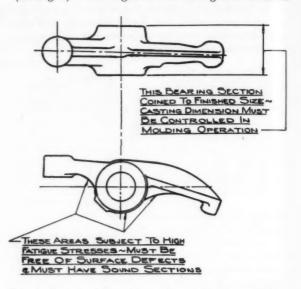


Fig. 3-Automotive engine valve rocker arm casting.

to the finished dimension at the customer's plant. Therefore, it is essential that the amount of finish on these surfaces be closely controlled. By making frequent daily checks on white iron castings, with templates (made to the proper expansion rule), the foundry inspector maintains this requirement on the rocker castings consistently.

He must keep a close vigilance on the condition of the pattern equipment and the molding operation because if the mold is not rammed correctly, the casting is subject to internal blows or scabs in the bearing area. Usually this condition does not appear until after the inside diameter is machined. Defects of this nature are prohibitive on a finished bearing surface.

The end use of a rocker arm exposes the part to considerable stress and fatigue and requires a casting to be sound of section and free of defects on the surfaces that are under tensional load when in use. Therefore, daily periodic checks by cutting or breaking white iron castings is also part of the inspection routine to avoid large quantities of scrap that may unknowingly occur from contributory foundry conditions.

Setting and maintaining the quality standards on white iron inspection is another duty for the foundry inspector. With his familiarity on quality in the foundry, he is in a position to control effectively the standards at this point of examination. White iron inspection is also a key source of information for the inspector to determine which jobs need immediate

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Gating problems may also be handled by the foundry inspector; however, it is more advantageous to all operations for a gating specialist responsible to the foundry proper to handle this work.

White Iron Inspection

There are many advantages in placing an inspection operation on white iron castings for obvious defects before they are annealed.

This generally requires 100 per cent inspection of the individual castings and can be done by a workman in the operating department in conjunction with his regular production job.

For example, the removal of sprue involves the individual handling of castings. The operator doing this work also makes an examination for obvious defects.

In a mechanized plant having a conveyor belt to carry the parts to the sprue operation, the worker at this point removes all castings of doubtful quality. These castings are placed on a second belt that carries them to a white iron inspector. This inspector then decides if the parts are definitely scrap.

After the defective white iron castings have been rejected, they are removed from the operating area. Periodically the pieces are resorted and counted according to their respective defects and a record is made of each casting with the quantities of each type of defect.

This gives the foundry an early analysis of their scrap and is one of their guides in eliminating large quantities of defective work. The inspection standards used by the white iron inspector are checked and maintained regularly by the foundry inspector.

Grind Inspection

If the volume of production is large on pearlitic castings, it often warrants placing a member of the inspection department in the grinding room.

On numerous end uses the finished part is subjected to close clearances in assembly and fins or gates of any appreciable height are prohibitive; therefore, the proper grind on most pearlitic castings is important.

The main objective of placing a member of the inspection department at this point is to reduce the quantity of rejects on the finished iron inspection that would require salvage grind and also, the chances are reduced of improperly ground castings going to the customer's plant. An inspection department member can control the quality of workmanship by making daily periodic examinations on each of the operations. Usually, snag grinding of this type is made without fixtures to control dimensions; therefore, on a large scale operation considerable salvage and scrap can be avoided if the different operations are kept under constant quality surveillance.

Finished Iron Inspection

Maintaining Dimensional Tolerances—One of the initial interests of finished inspection on pearlitic castings is the maintaining of dimensions, within their tolerances. Foundry operations usually control the finished dimensions on castings that hold their shape

during annealing and do not require a corrective operation. Other dimensional changes such as strains and swells can be segregated by visual inspection.

If a casting is subject to distortion during annealing or handling, a corrective die pressing operation is necessary. If the straightening operation returns the warped casting to its dimensional requirement, then this operation can be also considered as a gaging operation.

However, many castings are designed with extended thin sections which during pressing will deflect into place when the straightening die closes over, but returns out-of-tolerance again when the die pressure is released. This is due to the high yield strength of the pearlitic iron. Although some correction may have been made in the pressing operation, it usually becomes necessary to gage each casting individually and hand straighten.

In many applications, because of the cost, these operations are prohibitive and a means of positive correction becomes essential.

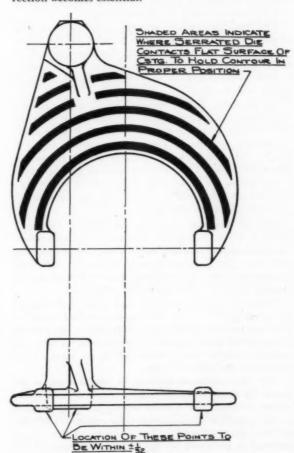


Fig. 4-Transmission shifter fork casting.

The pressing of a shifter fork (see Fig. 4) used in a truck transmission is a good example of a casting deflecting in the pressing operation. The original die for the part was a full surface contact type fixture. The dimensional tolerances permitted on the casting was a plus or minus $\frac{1}{32}$ in. However, at the peak

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press capacity of the 500 tons total pressure, 3/32 in. warpage was still encountered and 100 per cent gaging

became necessary.

To die straighten this fork casting within the required tolerances, one of the contact faces was serrated. Where the serration contacted the casting, the pressure per square unit area was increased to more than twice the original pressure. The indentations made by the serrations set the metal sufficiently to hold the casting within the required tolerances. Hence, a means of accurate straightening eliminated the necessity of an individual gaging operation.

It is good practice after a straightening die is placed into service, to allow the inspection department to make all final decisions on the fixture. In this manner the accuracy of straightening dies is maintained in

the same manner as gages.

Visual Inspection—Visual inspection of finished castings is probably the most controversial phase of any inspection operation, due to the fact that the results obtained are entirely controlled by the human element and the visual ability of an individual.

The final application, general design, and method of molding generally determine to what extent a casting will have to be examined. Castings applied to static end uses and do not have defects that are undetectable to the naked eye, may be inspected by ordinary

visual examination.

For example, if the casting is small and made on large machine type molding equipment, it may be more subject to strain and shift than if it were made on smaller equipment. Therefore, observing the casting for shift and fins will be one of the important items to look for. This becomes increasingly important if the defects are on an area of the casting where they will not machine out or happen to be used in an assembly where the clearances are close.

Induced Magnetic Testings for Surface Defects— Many pearlitic castings are applied to end uses where the finished part is under high stress and fatigue that will often run into millions of cycles. It, therefore, becomes necessary to emphasize surface defects on the castings that are not detectable otherwise.

The most common method used today is the inducing of magnetism into the casting so that opposite poles are formed at each edge of the defect. By placing compounded iron particles on the casting surface, the particles outline the polarity which, in turn, is the

outline of the surface defect.

The most common method of magnetizing is induced by direct current usually rectified from alternating current. The surface defects that lay parallel to the current which is also at right angles to the lines of force, are emphasized. The intensity of the defects outlined is governed by the surface condition of the casting being examined and the magnitude of the defect itself.

The writer is not an authority on this method of examination and does not wish to take part in any controversial question with experts in this field. A great deal of the following information is contrary to the theory of good practice, however, much substantiating evidence was produced to confirm and justify using such a method.

The controversial method is based on the same principle just covered, except that the alternating current is not rectified to direct current, but stepped down to the desired voltage and amperage (8 v-4000 to 5000 amp) and remains alternating.

The results of this type of current not only shows defects parallel to the current, but also the surface flaws that are considerably off parallel. This is an ad-

vantage when checking uneven contours.

Another apparent improvement, at least on rough casting surfaces, is the improved contrast of the defect. On castings that have been previously tested by the conventional "set-up," a standard of allowances had to be made on the AC system because of the severeness of detection.

Defects that did not appear on the DC, were obvious on the AC and cracks that appeared on the former were so great on the latter that it was difficult at first

to determine a good casting.

The control of the current on the AC set-up is maintained with a de-ion breaker installed on the low voltage side. This breaks the current on the zero position of the sine wave and apparently eliminates any surge of current that would demagnetize the casting.

The most common methods of applying the iron compounded particles is in the dry powder form and prepared particles that are suspended in an oil-base solution. The solution method appears to be most widely used in recent years, because on most applications it offers a more uniform distribution of iron particles over a casting surface. With both methods the parts are examined with the naked eye.

Another method is the use of iron particles in an oil base solution, prepared for examining defects under a violet-ray light. This method gives considerably greater detection than the other methods using

the naked eye.

The writer recently witnessed a test on a water-base suspension, instead of oil, for use under a violet-ray light, which appears to have some advantages. The contrast of the defect appeared to be as clear and perhaps a bit sharper than when examined with the oil solution. Reduction of fire hazard and industrial skin diseases are items also in its favor.

Hardness Testing—Controlled hardness of the iron is a major qualification for pearlitic castings that are highly stressed or subject to considerable fatigue. Also, on many applications, the parts are localized hardened (usually salt bath, induction or flame methods) or quenched homogeneously and tempered at the customer's to a desired hardness.

For pearlitic malleable to readily respond to these heat treating processes, it is important that the percentage of pearlite in the iron's metallographic structure be held to predetermined ranges. It is also this percentage of pearlite that determines the hardness of the annealed iron; therefore, the amount of pearlite will be determined approximately by testing the hardness of the annealed iron.

The ball impression method of testing is probably the most common method used for testing hardness in the foundry industry. Dial-type machines are usually used for high production work. The accuracy of reading the dial must depend upon the human element. GS

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Often because of fatigue or other causes, an error of observation is made by the operator and parts outside of the required limits are passed as satisfactory.

This method also requires a fairly flat and smoothly ground surface on a section having sufficient support to withstand the test load. If the supporting section deforms, then the hardness reading is inaccurate. For castings of a moderate or larger size, this method is satisfactory, although the test impression destroys the surface in a small area.

On castings of a smaller nature, and often with cored bearing sections, it is usually difficult to find sufficient area or section to Brinell without making the part unusable. Therefore, a means of hardness testing without deformation of small pearlitic castings is a desirable item.

It has been the writer's good fortune to observe during the past few years, the development and finally the full application of an electronic method of hardness testing for automobile engine valve rocker arms.

The principle of this method of hardness testing is based on the ability of the iron to retain magnetism. The magnetic retentivity is a function of the retained pearlite in the iron's structure, or in other words, the more pearlite present in the casting, the greater the ability of the iron to retain the magnetism when magnetized by a constant magnetizing force.

By measuring the retained magnetism, the amount of pearlite is determined; therefore, the hardness is determined because the amount of pearlite present

governs the hardness of the iron.

Applying this principle, the function of the testing machine works as a comparator. By calibrating the machine with castings of known hardness that are duplicates of the castings to be tested, the machine then tests the parts on a comparative basis. Pieces that are outside of the required range by 0.10 mm Brinell reading, are rejected. The castings that are within the calibrator ranges are passed as acceptable.

Gravity fall is used in the mechanics of the machine to carry the casting from its first phase of operation

through until it is passed or rejected.

A tube of sufficient diameter is used to guide the casting through the different steps of testing. The castings are fed into the entrance end of the tube which is on a decline and continues to a section that is in a vertical position. (See Fig. 5) Located in the declining tube is an initiating coil. As the castings pass this coil, a current is induced that causes a switch to close that energizes the magnetizing coil located in the upper part of the vertical tube.

Gravity carries the casting down until it reaches the magnetizing coil, where it is held suspended in the field until magnetically saturated. (0.35 sec was found to be sufficient time to saturate the castings being

tested at the time of the writing.)

The magnetizing coil has the force to pull the casting into its magnetic center for saturation. If the piece falls too far, the coil will pull it back, or if it has not fallen far enough, the coil will put it down.

Therefore, the saturation is uniformly controlled and each casting will drop from the same position at exactly the same height. The saturation period is controlled by an electronic timer that cuts off the magnetizing coil so that the casting is free to continue its gravity fall down to a selector (butter-fly) valve located at the lower end of the tube. This valve either rejects or passes the piece.

The butterfly selector valve is always set in the reject position so if the current fails or mechanical trouble is encountered, the casting will be rejected. The valve control is achieved by a double wound pick-up coil located in the tube between the magnetizing coil and

the butterfly valve.

The free-falling casting as it passes the pick-up coil, induces a minute current in the coil, because the falling part is magnetized. The minute current is then amplified and passed through thyratron tubes, which in turn, operates relays that control the butterfly selector valve. The first winding of the pick-up coil is set to the low limit—the second winding to the high limit.

The circuits of both windings are arranged so that when there is no induced current in either winding, the butterfly remains in the reject position. When a casting is soft, its retained magnetism is low; therefore, when it passes the coil, it will not induce current in either winding, and the butterfly remains in the reject position.

If current is induced in the low range winding only, the butterfly opens to the O.K. position. Hence, a casting within the proper hardness range retains enough magnetism to induce current in the low range winding only, but not in the high range. Therefore, the butterfly is opened to the O.K. position and the

part is passed.

The current from the pick-up coil is arranged so that when both windings are induced together, the butterfly remains in the reject position. If a casting is harder than the calibrated range, it will be high in retained magnetism so that in passing the coil, it will not only induce current in the low range winding, but also induce current in the upper range winding, and

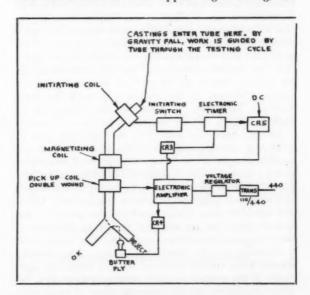


Fig. 5—Schematic diagram of electronic hardness testers.

the butterfly holds the reject position.

After the castings go through the butterfly valve, they pass through a de-energizing coil and are demagnitized. The parts that pass as O.K. come out of a separate chute from those rejected. Little handling is required because the good pieces can be carried to the O.K. hopper by a small conveyor belt. (See Fig. 6.) The rejects coming out of a second chute are retained in a separate container until disposed of.

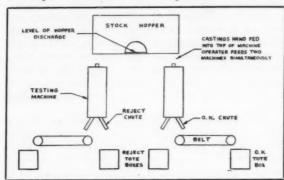


Fig. 6-Layout of operation of electronic hardness testing.

This method of hardness inspection eliminates the grinding of test surfaces and the error of observation in testing. Also, the casting is free of any deformation. Before the event of electronic testing, a worker was required to grind a test surface and an operator to test the hardness, with an output of approximately 1000 pieces per hour. With the new method, an operator can operate two machines simultaneously and test 5000 pieces per hour or more, with greater accu-

Salvage and Returned Goods Inspection—Proper use of salvage inspection can save a foundry untold expense by reclaiming usable castings rejected by its plant operations and from customer's returned goods. Castings that are rejected at the finished iron inspection are not always scrap and should be sent through a salvage inspection operation. Often a part may be rejected for a given defect, but the extent of the defect may not warrant scrapping of the part. Such decisions on quality cannot be expected of the production inspector, because of the volume of work he must handle. Therefore, to eliminate the scrapping of usable castings, salvage inspection becomes necessary.

Salvage inspection also plays an important part in analyzing castings that have been returned from the customer as unusable. Every lot returned should be given a pilot salvage examination to determine if the parts can be saved or what is necessary in the foundry's scope of operation to correct the trouble. Often the machine shop rejects machined or partially machined parts that are analyzed as casting discrepancy, but through close salvage inspection, it is found to be a variation in the machining set-up. The foundry should keep a close vigilance on this condition especially if the work is of high volume production, because usually we can all make our share of scrap without help from someone else.

Conclusion

Acceptance of pearlitic malleable castings in new fields of application is based on quality. Casting inspection plays an important part in any foundry organization. Its effectiveness governs the quality of the finished product, which in turn is often a determining factor for future business and expansion.

Pearlitic malleable castings are considered a rough material in which the cost will be limited to meet present-day competition in other fields; therefore, it is essential to use the most economical, but effective. methods of casting inspection.

DISCUSSION

Chairman: H. Hunt, Auto Specialties Mfg. Co., St. Joseph,

Co-Chairman: F. WURSCHER, Marion Malleable Iron Co., Marion, Ind.

ERIC WELANDER: 1 In Fig. 3 the author refers to the coining operation on an automotive engine valve rocker arm casting. How much reduction of thickness are you able to obtain in a casting of that nature by coining?

Mr. MARTIN: We try to hold to approximately 0.025 in. but it will vary.

MR. WELANDER: What pressure is used? Mr. Martin: Approximately 600 tons.

MILTON TILLEY: 8 Referring to the magnetic hardness tester, as I understand it when the casting passes the first coil it is magnetized. When passing the second coil, the magnetism retained in the casting operates the reject or okay mechanism, either too soft or too hard. Is that right?

Mr. Martin: That is right. Richard Schneidewind: ⁸ You oil quench and temper some castings and air quench and temper others. Certainly the same magnetic setting is not going to give you the same hardness in each case. Do you have to calibrate the pick-up device for each casting and its method of heat treatment?

MR. MARTIN: Yes, we do. The testing is set up for a certain set of conditions and operates as a comparator. This is done by adjusting the machine to castings that are duplicates both metallurgically and dimensionally of the castings to be tested.

A. S. JOHNSON: 4 I understand that the different types of pearlite, lamellar or spheroidal, has an effect on the amount of magnetism that is retained.

MR. MARTIN: Yes, no doubt the type of pearlite has some effect. However, after the annealing or heat treating cycles have been established and you have the proper metallurgical control, I do not believe there is excessive variation encountered in that respect. However, certain exceptional conditions such as heat treating cycle variation will cause a variation on the comparative hardness testing.

J. E. REHDER: 5 In my experience in these magnetic methods of detecting pearlite or hardness in malleable castings, there is quite a pronounced skin effect. In this particular instrument do you have any trouble testing pearlitic malleable castings that have a slight decarburized skin?

MR. MARTIN: I will not be able to answer that too positively. Assuming that all castings supposedly go through the same heat and cooling cycle, the comparative testing, no doubt, will detect excessive decarburization. Slight decarburization does not effect the testing efficiency to the extent that it is prohibitive for high volume operation. I do not recall if tests have been made to determine the amount of decarburization necessary to influence the test results.

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Metallurgist, Union Malleable Iron Works, East Moline, IH.
 Metallurgist, National Malleable & Steel Castings Co., Cleveland.
 Prof. of Metallurgical Engr., University of Michigan, Ann Arbor,

Metallurgist, The Lake City Malleable Co., Cleveland.

⁸ Foundry Engineer, Canadian Bureau of Mines, Ottawa, Canada.

PRODUCTION PATTERNS AND THE MATCHPLATE

By

Robert F. Dalton*

ABSTRACT

Chronological development of production pattern equipment should be of interest to many foundrymen. Advantages and disadvantages of various types of pattern equipment are described and enumerated. Use of matchplates in the foundry is determined by (1) production requirements and costs, (2) size of mold to be handled, and (3) reduced degree of skill required in molding.

Because of high degree of accuracy obtained in manufacture of pressure cast aluminum matchplates, a better knowledge of some of the properties of formulated metal casting plaster as a molding material may be helpful in the production of other non-ferrous castings.

Introduction

SINCE 1932 the aluminum pressure cast matchplate industry has grown to be a necessary adjunct to the metal casting trade. Many thousands of plates are in daily use and little thought is given to them by the user. The matchplate is a practical approach to precision casting. The possibility of applying the principles of the matchplate industry to other non-ferrous foundry problems should be of interest.

A survey of the literature reveals that although there are numerous articles published on the manufacture and use of matchplates, little is available as to the economies of when to buy and use them.

Types of Pattern Equipment

Development of the matchplate and the final evolution of the specialty matchplate foundry is an interesting page of the foundry industry. There are a variety of ways in which a pattern may be made and used in the foundry. They may be briefly enumerated as follows:

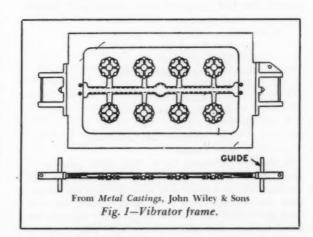
1. Loose pattern (solid)—The ordinary single piece pattern is, of course, as old as the foundry industry itself. With low production requirements, it is the cheapest form to produce.

2. Loose pattern (split)—The pattern may be split at a flat parting line, thus facilitating (a) making the parting and (b) drawing the pattern during the molding operation.

3. Gated pattern—One or more patterns of a similar or dissimilar shape may be joined together with a common gate or runner. It is generally used in conjunction with a green sand or hard sand match. Gated patterns are expensive and require a high degree of skill in their manufacture. Each pattern is made individually, cleaned and soldered to a common runner. Many times the gate is made larger than is practical for pouring purposes so that breakage of the assembly will be minimized. The gated pattern is usually used as a master in the production of sand cast matchplates. An early use of the hard sand match ("offside") with gated patterns was described by F. Turner and D. Town⁴ in 1914.

4. Vibrator Frame—A gated pattern may be attached to a vibrator frame. The assembly is used in conjunction with a sand match in the same manner as a gated or loose pattern. Pin guides and a vibrator are attached to the frame, permitting accurate pattern withdrawal. This method of patterning was described by E. S. Carman⁵ in 1919. See Fig. 1.

5. Drop Machine—The drop machine or stripping plate was the first high production method of making sand molds. The stripping plate provides a means of drawing ("dropping") the pattern through a contoured plate. Manufacture of this type of pattern



^{*} Development Engineer, United States Gypsum Company, Chicago.

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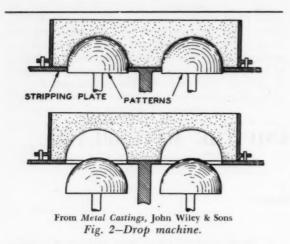
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equipment is expensive since the stripping plate must have a close fit in order to provide a good draw and an accurate mold parting. See Fig. 2.

6. Matchplates—The modern pressure cast aluminum matchplate is the result of an interesting evolutionary process. One might regard the matchplate as a pattern with a heavy fin at the parting line extending to the outer portion of the flask. One might surmise that the first matchplate was the result of the examination of a scrapped casting due to a lifted cope by some imaginative foundrymen. The manufacture of iron matchplates cast in sand molds was described by A. McWilliams and L. Longmuir¹ in 1907 and E. L. Rhead² in 1909.

The development of the matchplate made possible the tremendous increase in production due to the full usage of the power operated jolt-squeeze molding machine. Magazine articles and books relating to the use of matchplates were written by F. Turner and D. Town⁴ in 1914, P. Dwyer⁷ in 1929, H. Magdalenat⁹ in 1930, F. G. Butters¹⁰ in 1941 and many others. Most of the published literature deals with the use of patterns (including matchplates) in the foundry.

A distinction should be made between the mounted pattern and the integrally cast matchplate. Matchplates may be divided into two classes—

A. Pattern mounted on a plate.

- (1) Wood patterns on a plate.
- (2) Metal patterns, etc.
- B. Integrally Cast Matchplates.
 - (1) Sand cast
 - (a) Aluminum
 - (b) Plaster, etc.
 - (2) Plaster Cast
 - (a) Pressure cast aluminum
 - (b) Plastic

A. Pattern Mounted on a Plate—Split patterns of wood, metal or other suitable materials are mounted mechanically to a plate that is larger than the desired flask size. Patterns may be of good wearing material, such as brass or cast iron, with excellent machine finished surfaces and mounted on light plates such as magnesium or aluminum. This is the most accurate method of eliminating shift in the matchplate and

cope and drag equipment. Split wood patterns may be mounted on wood or metal plates with a minimum expenditure of money. Use of cores in the mold may eliminate the problem of uneven parting lines. Various problems encountered in mounting patterns was described by H. N. Tuttle⁶ in 1926. Mounted matchplates were also described by E. S. Carman⁵ in 1919, E. W. Horlebein¹¹ in 1942 and others.¹²

B. Integrally Cast Matchplates—Casting of aluminum matchplates in sand molds was an early development and many thousands of them are in daily use in the foundry. A complete gated pattern with double shrinkage is required together with a highly skilled molder. The cost of producing the plate is largely determined by the original master pattern cost and the skill of the molder. An early description of the process was written by R. H. Palmer³ in 1912. For castings generally of small size and low production requirements, matchplates may be cast in sand molds of plaster or gypsum cement, suitably reinforced, similar to the vibrator frame as shown in Fig. 1.

The casting of non-ferrous metals in plaster molds is an ancient art. It is only in the past 30 years that sufficient ingenuity and skill was acquired to successfully cast an integral aluminum matchplate in a plaster mold. In the past 10 years the pressure cast aluminum matchplate has found wide acceptance in the foundry industry. One of the distinct advantages in the production of multiple pattern pressure cast matchplates is the relatively low cost of the master pattern. Sand cast matchplates require full gated masters whereas in plaster molding one or two master patterns will suffice in most cases. Thus the cost of a full gated master pattern may be more than the entire cost of the multiple pattern pressure cast matchplate. The commercial casting of aluminum matchplates is described by J. P. Callahan,13 E. Bremer,15 S. N. Touchman,16 and others. See Fig. 3 and 4.

As in many industrial fields, use of plastics has invaded patterning. The claim is made that little, if any, shrinkage occurs in converting other types of patterns to plastic. This property could be used to good advantage in using present pattern equipment for the production of matchplates. In making aluminum matchplates a master pattern with double shrinkage is required. The manufacture of plastic matchplates in plaster molds was described by C. R. Simmons¹⁴ in 1946.

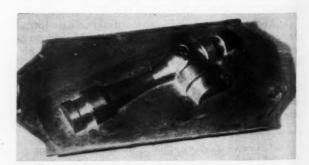


Fig. 3-Single impression matchplate. Pressure cast in a plaster mold.

7. Cope and Drag Patterns—Use of separate cope and drag patterns was described by Bornstein⁸ and others. Half plates with integrally cast patterns made of cast iron were used to increase production, replacing hand or gated patterns in a hard sand or plaster match. The work involved in making molds may be divided among several men. For the production of large molds, especially on molding units, the cope and drag pattern is extensively used. See Fig. 5.

8. Other types-Stack, centerline and coremolding are also used in pattern tooling for the production

foundry.

Stack molding is similar to cope and drag except that the drag pattern is mounted to the squeezer head. Thus a complete mold is made with the cope portion on the bottom of the mold and the drag portion on the top.

In centerline molding the cope and drag patterns are mounted on the same plate. They are symmetrical to the pin centerline. Thus one flask covering the entire plate will make a mold of half "copes and drags" and will be used as a drag. The next flask will then be used as a cope and reversed in closing over the pins

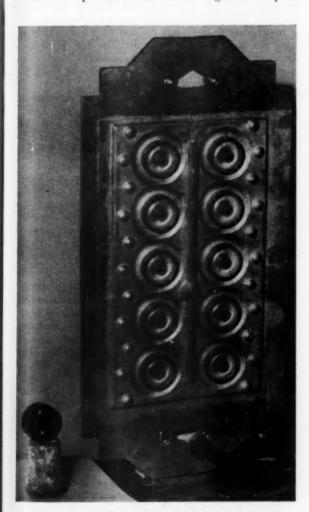


Fig. 4-Multiple impression matchplate. Made from single master pattern. Cast in plaster mold.

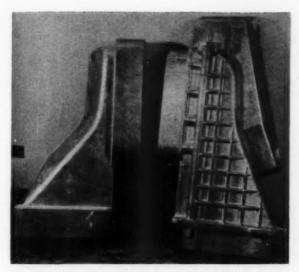


Fig. 5-Cope and drag patterns. Pressure cast in plaster molds.

to form the complete mold. There is no difference in the shape of the cope or drag halves of the mold except for the sprue or any necessary risers in the cope.

The entire mold may be made of cores. Patterning may be simplified by the use of segment cores and scrap reduced because of the relatively greater safety of us-

ing cores instead of green sand.

The inherent qualities found in the process of the pressure cast aluminum matchplate pattern are being used to good advantage in the manufacture of other patterning equipment. Other types of equipment being made by this method are:

1. Loose patterns.

2. Cope and Drag patterns.

3. Patterns for mounting on matchplates.

4. Core boxes and driers.

A booked core box made by this method is shown in Fig. 6. A pattern the shape of desired corebox would be expensive but by a relatively new process, the castings were made using a "plug," the shape of the desired core and inexpensive wooden patterns to form the back of the box.

Economics of Use of Matchplates

As is the case of other production patterns the matchplate has a definite place in foundry pattern tooling. The ability to determine when to use a matchplate or other types of pattern equipment depends on the individual foundries ability to accurately predetermine costs. This should be done from the results actually obtained in production with patterns and flasks of a similar nature. Factors important in the use of matchplates might be enumerated as follows:

1. Production requirement.

2. Replacement of old equipment.

3. Degree of skill required in molding.

4. Dimensional tolerances.

5. Size limitations.

1. Production Requirements-The matchplate is

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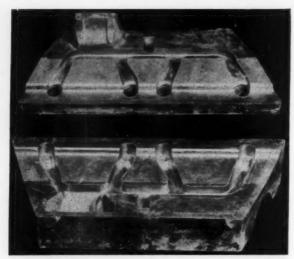


Fig. 6-Booked type core box. Pressure cast in plaster molds.

recognized as a production pattern and it may be considered to be used above the production requirement of a gated pattern and below that of cope and drag work. The pattern cost must be pro-rated according to the number of pieces or molds to be made. If the pattern cost cannot be absorbed in less than two years casting requirements (this figure may vary) then it may be advisable to use some other type of pattern equipment such as loose, gated, etc.

A typical example is shown in Table 1.

TABLE 1

| Pattern Equipment | Gate of 1 | Matchplate |
|---------------------------------------|-----------|------------|
| Flask size (inches) | 12 x 18 | 12 x 18 |
| Pieces per mold | 1 | 1 |
| Molds per day | 50 | 120 |
| Molding cost per piece | .24 | .10 |
| Cost of plate | | \$72.00 |
| Saving per mold | | .14 |
| Order required to pay for plate (pcs) | | 514 |

If the customer requires 500 pieces per year the use of the matchplate would be justified. If the production requirement was only 100 pieces per year then the use of a matchplate would not be practical from a purely economic standpoint.

A typical example of where a matchplate replaced a drop machine pattern is shown in Table 2.

TABLE 2

| Pattern Equipment | Drop Machine | e Matchplate |
|---------------------------------------|--------------|--------------|
| Production requirement (yearly) | 12,000 pcs | 12,000 pcs |
| Flask size (inches) | 14 x 14 | 14 x 14 |
| Pieces per mold | 4 | 4 |
| Molds per day | 100 | 250 |
| Molding cost per mold | .12 | .048 |
| Total Molding Cost | \$360.00 | \$144.00 |
| Cost of plate | | \$ 90.00 |
| Time to produce order (days) | 30 | 12 |
| Order required to pay for plate (pcs) | | 5000 |

Sometimes the savings in molding cost may be difficult to determine since the transition to a matchplate may be accompanied by changes in molding methods such as putting the "job on a line." The problems of storage and flask repair are minimized in the use of the matchplate compared to drop machines.

2. Replacement of Old Equipment—Many foundries have had gated patterns and drop machine patterns in productive use for a considerable period of time. The maintenance cost of these types of equipment is much higher than the matchplate. With the drop machine pattern the replacement of the contoured plate may be more than the cost of a new matchplate.

The gated pattern, with low daily production, because of increased time in making the mold, does not "fit" into the high production conveyor line foundry system. Each pattern on the line will have to bear its share of the cost and the low production of the gated pattern would place a high overhead cost, per piece, when using this type of equipment.

3. Shill Required in Molding—The matchplate has done much to decrease the degree of skill required in the molding trade. Whereas the apprenticeship time for a journeyman molder is four years a squeezer machine operator (especially one making the same mold for long periods of time) may learn to mold with a matchplate in a matter of days or weeks. This is due, not alone to the matchplate, but to all of the improved methods of increasing production and making better castings. A few of these might be listed as follows: (a) the vibrator, (b) synthetic sand and sand handling equipment, (c) better molding machines, (d) better flask equipment.

The wide acceptance of the matchplate and the molding machine has also placed many foundries in the position of accepting only matchplate work for a majority of their production.

4. Dimensional Tolerances—In high speed multiple machine operations such as boring, turning, tapping, etc., the casting must be "true" to pattern. The matchplate and other accepted pattern equipment together with "modern" foundry equipment has helped to meet this demand. The discard of the rapping bar, hand drawing and patching the mold and the hard sand match has been made possible through the substitution of the hand and gated pattern for one matchplate.

5. Size Limitations—The matchplate has been accepted as a "one man job" and is applied generally to the making of small molds. When a larger size mold is required other methods of production, such as cope and drag, plates, are used. There are, of course, exceptions where large matchplates are used with a molder and a helper.

Weight of a mold that a molder can handle to the best advantage will, of course, vary with the size and strength of the individual. In a particular foundry, they have standardized on 1900 cubic inches of sand, or roughly 110 lb, as the size limitation of one man jobs. In the same shop, molds of approximately four times this weight are being made with cope and drag equipment by two men handling half the mold at a time.

Tables 1 and 2 show typical examples and savings of this type may not be possible on all jobs. Production requirements may be too low to pay the cost of ds

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the plate in a reasonable length of time. However, the increased cost of making molds with the use of drop machines and gated patterns is forcing the found-ryman to adopt better pattern equipment such as the matchplate and cope and drag pattern.

In order to take advantage of cost reductions of this type, foundrymen *must* become more cost conscious. The demand for reduced prices by the casting consumer and increased competition among casting producers will force foundrymen to economies of this type in order for them to survive in the business world.

Use of Formulated Metal Casting Plaster in Matchplate Industry

A. Advantages—The basic characteristics of plaster as a molding material have long been recognized. Some of the reasons for the popular use of formulated plaster are as follows:

1. Convenience-Plaster slurries are readily mixed and easy to handle.

2. Bonding ability—Set plaster provides strength so that the mold will hold its "shape" while casting.

- 3. Fine detail and good surface. Plaster slurries retain fine detail on setting and subsequent casting. Some idea of the detail available in plaster molding might be imagined by the sand foundryman if he were able to make molds with a material as fine as silication.
- 4. Easy to control. Plaster is a practically pure material which may be mixed with other controllable materials to create a wide range of desirable properties.
- 5. Cheap and abundant. Since earliest times plaster has been widely available and relatively inexpensive.
- 6. Versatile—Wet plaster has considerable strength and excellent bonding quality. Thus, many times in making matchplates only one master pattern is required. Plaster segments are made and later assembled to produce multi-pattern plates.

B. Disadvantages-

1. Higher Cost—In plaster molding sufficient accuracy is often obtainable to eliminate or minimize machining operations. This may lead to a decreased cost for the machined part when compared to a sand casting, with relatively high machining costs. Desired smoothness of a non-machined surface may require that the casting be made in a plaster mold.

2. Material is not recoverable. As in most core sand molds, plaster is generally not recoverable.

3. Low Permeability. Plaster molds with a few exceptions, are low in their ability to vent gases. The exceptions to the low permeability characteristic are the Morris Bean Process (Patent 2,220,703) and P-M-C plaster (Permeable Metal Casting Plaster) described by K. Miericke and E. Johnson. In the Bean process permeability is attained through a treatment of the mold. With P.M.C. plaster, permeability is obtained through the introduction of many fine bubbles of air in the slurry during the mixing operation. When the plaster "sets" the air bubbles rupture and form continuous networks of air channels throughout the plaster mold. In regular matchplate plaster the permeability is near one A.F.S. unit, thus:—

4. Mold must be calcined—This requires oven equipment and increases the time of processing. The

mold may not be completely dry, but a low water content is desirable.

5. Plaster is considered as a specialty—The use of plaster as a molding material has been regarded as an art by the average sand foundryman. Sand cast matchplates are being made in a number of foundries, but pressure cast matchplates in plaster molds are generally made in specialty shops.

C. Characteristics—In speaking of plaster and plaster slurries the foundryman should become acquainted with the term: "consistency." Consistency is the parts of water used with 100 parts of plaster (or any dry formulation). The term: "consistency," is to plaster as the term "moisture" is to sand.

Matchplate plaster is a mixture of gypsum cement and a fibrous refractory material in a ratio of 3 to 5 parts of gypsum to 1 part of the fibrous refractory. This mixture is necessary to give coherence to the calcined mold and to permit the use of higher consistencies in order to obtain maximum permeabilities. "Normal" consistency for a plaster may be up to 100 but the "use" consistency of matchplate plaster is 140 to 160 and in some cases as high as 180.

The physical properties of a typical matchplate plaster mold are shown in Table 3.

TABLE 3

| Consistency: 140- | -160 | |
|----------------------------|------|-----|
| | Wet | Dry |
| Mold density (lb/C.F.) | 85. | 33. |
| Compressive strength (psi) | 95 | 50 |
| Permeability (A.F.S.) | 0 | .€ |

A description of the process is not within the intent of this paper, although the manufacture of pressure cast aluminum matchplates in plaster molds is well described in the literature by J. P. Callahan, ¹⁸ E. Bremer¹⁵ and S. N. Touchman. ¹⁶

Acknowledgments

The writer wishes to express his appreciation to Mr. James Mathias and Mr. Orville Mercer of the Accurate Matchplate Company, Chicago, Mr. H. L. Campbell, Oak Park, Illinois, and Mr. Willis Hale, Scientific Cast Products Corp., Cleveland, for the use of their photographs and helpful information.

Conclusion

The pressure cast aluminum matchplate has found wide acceptance in the foundry industry. A more thorough study of patterning and molding costs will lead to an ever increasing use of this type of pattern tooling.

It should be remembered that the pressure cast aluminum matchplate pattern is a casting. A better understanding of the principles and methods involved in its production may be of value to the foundryman in the production of other non-ferrous castings. The recent availability of metal casting plasters with higher permeabilities should be of value in this direction.

The use of the pressure cast aluminum matchplate, together with other improved methods of production, has enabled the foundryman to produce better castings, at lower costs, in an ever increasing volume.

TABLE 4—LARGE GRAY IRON PRODUCTION FOUNDRY DATA SHOWING MATCHPLATE VS OTHER TYPES OF PATTERN EQUIPMENT

| Pattern Equipment | Gated | Matchplate | Drop Machine | Matchplate | Drop Machine | Cope & Drag | Matchplate | Cope & Dra |
|--|-------|------------|-----------------|------------|-----------------|-------------|------------|-------------|
| Flask size, in. | 10x16 | 10x16 | 10x18 | 10x18 | 16x16 | 16x16 | 10x18 | 18x18 |
| Pieces per mold | 2 | 2 | 2 | 2 | 1 | 1 | 2 | 4 |
| Molds per day | 71 | 110 | 152 (2 men) | 110 | 109 (2 men) | 200 (2 men) | 133 | 210 (2 men) |
| Molding cost per piece | .0845 | .0546 | .08 | .046 | .11 | .06 | .045 | .0286 |
| Cost of plate | _ | \$60.00 | _ | \$60.00 | _ | \$120.00 | - | \$100.00 |
| Order required to pay for plate, pc | - | \$2000.00 | - | \$1760.00 | - | \$2400.00 | - | \$6120.00 |

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PROBLEMS IN PRODUCING DUCTILE IRON

By

Max Kuniansky*

ABOUT TWO YEARS have elapsed since Morrogh presented his paper¹ on spherulitic cast iron as obtained by treatment of very low sulphur and very low phosphorus hypereutectic cast irons with cerium or misch metal. About this time the International Nickel Co. made the announcement of its work with the addition of magnesium or its alloys for the production of ductile, nodular or spherulitic cast irons. This process worked equally well with hypo- as with hypereutectic cast irons. Also, the sulphur and phosphorus could be tolerated over a somewhat broader range. In the latter process, phosphorus can be held at an upper limit of 0.20 per cent and sulphur should be as low as possible.

In the very recent past, patents have been issued both to International Nickel Co. and to Morrogh. As my entire experience has been with the magnesium process, this paper will be devoted entirely to this

process.

Much work has been done in this field by Dr. James T. MacKenzie and Charles K. Donoho² of the American Cast Iron Pipe Co. and by Gosta Vennerholm of Ford Motor Company. T. E. Eagan of Cooper-Bessemer Corp. has also done a great deal of work in this field. There are now at least fifty licensees of the process pioneered by the International Nickel Co. and many of these are making notable contributions to the field. Much of the work in print to date deals with induction-melted irons and similar small batch-type melts. All our work has been of the cupola-melted metal and we have treated ladles ranging from 1200 lb of metal to 4000 lb. It is because of the widespread interest in this product that we thought we might deal with some of the practical aspects-particularly as regards raw materials and techniques for both the treatment with the magnesium alloy and the subsequent inoculation.

Most cupola cast irons are melted at spout temperatures of 2700 to 2800 F. It was early realized that the treatment with the magnesium alloy would require at least 1.50 per cent of solid addition. It was also found

that about three-quarters per cent of 75 per cent ferrosilicon was required as an inoculant. In all, it appears that 2.25 per cent solid alloy addition was the minimum we could get by with and still have metal of sufficiently high temperature to enable us to make sound castings. We also found that, to hold our alloy addition this low, we would have to keep the sulphur quite low. With this in mind, we cast about for suitable raw materials.

Typical of base materials we have thus far located are a charcoal pig iron and a low phosphorus-coke pig iron. A typical analysis of these pig irons is as follows:

| Element | Content, Per Cent | | | |
|--------------|-------------------|--|--|--|
| Silicon | 1.35-1.50 | | | |
| Sulphur | 0.025 | | | |
| Phesphorus | 0.025 | | | |
| Manganese | 0.40 | | | |
| Total Carbon | 4.30 | | | |

The coke pig iron can be purchased to the same specification. The silicon is held at this level so that the silicon in the magnesium alloy plus that in the ferrosilicon used as an inoculant will permit us to finish with about 2.40 to 2.80 per cent in the casting. Our melting practice involves using coke with as low sulphur as obtainable and further desulphurizing with fused soda ash after tapping.

The manganese is kept below 0.40 per cent in the hot metal because in the presence of low sulphurs in the final metal after treating with magnesium (0.005 to 0.010 per cent), the manganese acts as quite a sta-

bilizer.

The method of adding the magnesium alloy as well as the ferrosilicon becomes important because reladling is to be avoided since it contributes to excess loss of temperature. Discussed are two different methods for making the additions. At one of our foundries the hot metal is tapped from a reservoir so that it flows into a vertical refractory-lined downspout. At the top of this downspout we have a vibrating feeder for adding both the magnesium alloy and the ferrosilicon. We allow enough metal to flow through the downspout so that the 2000-lb receiving ladle is about one-quarter full. At this time the magnesium alloy enters the downspout just above the stream of

Vice President and General Manager, Lynchburg Foundry Co., Lynchburg, Va.

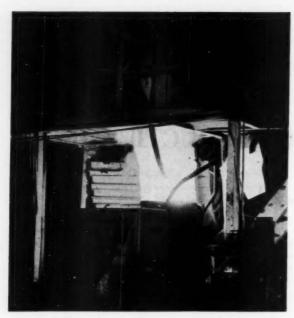
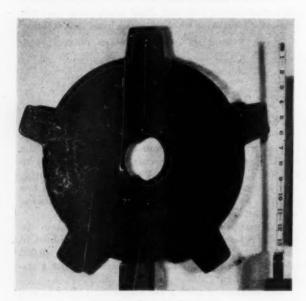


Fig. 1—Treating molten cast iron with magnesium alloys while transferring from a 10-ton T-pot ladle to 4000-lb capacity transfer ladle.



| | | Chemical | Analysis, | Per cent | | |
|------|-----------|----------|-----------|----------|-----------|-------|
| Si | S | Mn | P | T.C. | Ni | Mn |
| 2.11 | 0.007 | 0.38 | 0.050 | 4.12 | 0.75 | 0.072 |
| Cope | Side-6.41 | T.C. | | Drag Sid | le-3.60 T | C.C. |

| Physical | Data | |
|-----------------------------|-----------------|----------------------|
| Tensile Strength, psi | Casting 68,700* | 1-in. Keel 99,100 |
| Hardness, BHN | 217 | 228 |
| Elongation, per cent | 2.0 | 5.5 |
| Reduction in Area, per cent | 1.5 | 4.7 |
| * Unsound Bar | | |

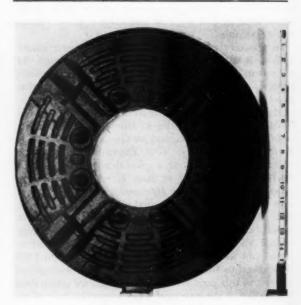
Fig. 2-Ductile iron clutch plate casting weighing 57 lb in which flotation of carbon to cope side was evident.



| | | Chemical | Analysis, | Per cent | | |
|-----------------------------|-------|----------|------------|----------|------|--------------|
| Si | S | Mn | P | T.C. | Ni | Mg |
| 2.97 | 0.015 | 0.33 | 0.055 | 3.59 | 0.64 | 0.092 |
| | | Phy | sical Data | a | | |
| | | | Clamp | 1-in. | Keel | 1/2-in. Keel |
| | | | Casting | Ba | r | Bar |
| Tensile Strength, psi | | | 100,800 | 97,5 | 00 | 102,800 |
| Hardness, BHN | | | 228 | | 35 | 228 |
| Elongation, per cent | | | | 5.0 | 6.5 | |
| Reduction in Area, per cent | | | 3.8 | 3 | 4.0 | 6.0 |

* Specimen too short to determine per cent elongation.

Fig. 3—Ductile iron flask clamp. Note chemical analysis and physical properties in as-cast state.



| | | Chemica | al Analysis, | Per Cent | | |
|--------|-------------|---------|--------------|----------|------|------------|
| Si | S | Mn | P | T.C. | Ni | Mg |
| 2.69 | 0.008 | 0.24 | 0.076 | 3.78 | 0.77 | 0.100 |
| | | 1 | Physical Da | ta | | |
| | | | | Casting | | 1-in. Keel |
| Tensil | e Strength, | psi | | 77,400 | | 82,900 |
| | ess, BHN | | | 179 | | 196 |
| Elonga | tion, per c | ent | | 12.0 | | 15.0 |
| | tion in Ar | | cent | 10.4 | | 11.9 |

Fig. 4-A 58-lb ductile iron clutch plate exhibiting high order of physical properties in specimen machined from casting.

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metal from the reservoir. Immediately after completion of this cycle the ferrosilicon that is used for the purpose of inoculation is added in the same manner. Our experience with this type of procedure has been quite satisfactory. There is no reladling and the use of this type of downspout causes very little fireworks in the plant.

Figure 1 shows the technique employed in another foundry of ours where we treat 4000 lb of metal at a time. The reservoir ladle which holds about 20,000 lb is tilted and when the 4000-lb ladle is about one-fourth full the magnesium alloy is added through the funnel arrangement so that it hits the iron in such a manner as to be constantly submerged in the bath of iron in the receiving ladle. When this operation is completed, the ferrosilicon is added immediately. This has proven quite satisfactory, but not so good as where we use a refractory-lined downspout. We could not employ this installation here because of lack of headroom and other considerations.

Almost all our work has been with hyper-eutectic irons. In sand castings, where hyper-eutecticity is quite pronounced, we get into trouble because of carbon migration. Figure 2 shows a clutch plate casting weighing 57 lb with uniform metal section of 13% in. As shown in the photograph, the ladle analysis indicated the presence of 4.12 per cent total carbon in the iron used for this particular casting. In the machining operation a difference in the appearance between the cope and drag surfaces was noted and in-

vestigation revealed that this was due to considerably higher total carbon in the copy side than in the drag. Actual analysis indicated 6.41 per cent total carbon in the cope and 3.60 per cent total carbon in the drag. Microscopic examination bears out the correctness of these findings. The hypo-eutectic irons show a much lesser tendency toward migration of the carbon nodules.

Figure 3 shows a flask clamp with its physical properties. Figure 4 shows a clutch plate that was quite sound and had good properties in the as-cast condition.

Figure 5 shows a piece of 6-in. ductile iron pipe cast in accordance with the DeLavaud process. It was originally 18 ft long with wall thickness of 0.25 in. The 6-ft length cut from the original had previously been burst under a hydrostatic pressure of 4600 lb with a corresponding bursting tensile strength of 58,900 psi. It was then put under a crane runway and a 6000-lb ball was dropped on it from a height of 20 ft. This illustrates the toughness and ductility of this material.

Figure 6 shows the appearance of 8-in. ductile cast iron pipe after bursting hydrostatically. The pipe merely bulged and opened for a short length. Attention is called to the fact that two of the four pipes shown in Fig. 6 have been electric welded in the middle. These are the one in the foreground and the second pipe back of it. No indication of failure in the welds was observed after completion of the burst-



Fig. 5-Piece of a 6-in ductile iron pipe after striking with 6000-lb ball from height of 20 ft.



Fig. 6-Four lengths of 8-in. ductile iron pipe after bursting hydrostatically. Pipe opened up for short

distance and bulged. Pipe in foreground and second pipe back of it have been welded at middle.

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Experimental work to date indicates that ductile cast iron pipe made centrifugally in water-cooled steel molds possess physical properties far exceeding those of regular cast iron pipe made by the same process. The tabulation below is a comparison of the chemical and physical properties of 8-in. ductile cast iron pipe and regular cast iron pipe.

COMPARISON—CHEMICAL AND PHYSICAL PROPERTIES
DUCTILE CAST IRON AND REGULAR CAST IRON
IN 8-IN, PIPE

| | Снеміс | AL ANA | LYSES, P | ER CENT | | |
|--|-------------|---------|---------------|------------|-----------|--------|
| | Si | S | Mn | P | TC | Mg |
| Ductile Iron | 2.89 | 0.006 | 0.27 | 0.062 | 3.98 | 0.05 |
| Regular Iron | 1.72 | 0.007 | 0.40 | 0.52 | 3.54 | - |
| | PHYSIC | CAL PRO | PERTIES | TESTS | | |
| | | | (| (No. 143) | | |
| | | | Du | ctile Iron | Regular | r Iron |
| Wall Tensile, psi | | | 67,305 | | 27,516 | |
| Elongation, per cent Talbot Strip Test | | | 4.5 | | N | one |
| Modulus of Ru | pture, psi | | 6 | 97,900 | | ,560 |
| Modulus of Ela | asticity, p | si | 3,310,000 | | 7,450,000 | |
| Bursting pressu | re, lb | | 4,900 | | 2,450 | |
| Bursting tensile, psi | | | 47,400 | | 24,950 | |
| Impact test-50-lb hammer Ring Crushing Test | | | 5 Feet-30 NF* | | 5 Feet-5* | |
| Modulus of Rupture, psi | | | 128,025 | | 57,765 | |

^{*} This means that pipe was struck 30 times with the 50-lb hammer from a height of 5 ft and no failure (NF) occurred.

^{••} This means that pipe was struck 5 times with the 50-lb hammer from a height of 5 ft at which time failure did occur.

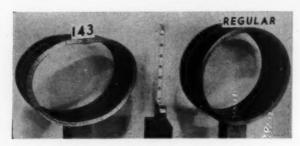


Fig. 7—Pieces of 8-in. pipe used for ring crushing tests.

Piece labelled "143" is ductile iron and other
is "Regular" iron.

Figure 7 shows pieces of the pipe whose properties are given in the above tabulation. These particular specimens were used for the Ring Crushing Tests, and the one at the left labelled "143" is ductile cast iron while the other is "regular" cast iron. Attention is called to the permanent set taken by the ductile iron pipe where a vertical deflection of 1.59 in. was recorded during test.

Figure 8 is a graph showing tensile strength-elongation-Brinell hardness relationship obtained in inoculated ductile irons produced in our foundries. All data were obtained from 1-in. Keel bars cooled as indicated. With the use of this graph it has been possible to predict with a reasonable degree of accuracy tensile strength and elongation when Brinell hardness is known.

All of our work to date has been confined to making castings in sand in such a manner that they will have measurable ductility without subsequent heat treatment. This can be accomplished if the following basic conditions are adhered to:

 Manganese must be kept below 0.40 per cent in the casting.

2. Sulphur must be as low as possible.

 Total carbon should be between 3.60 per cent and 4.00 per cent for castings of minimum thickness of 1/4 in.

4. The iron after treatment with the magnesium alloy must be inoculated with either a 75 per cent or 90 per cent ferrosilicon to avoid the formation of massive cementite.

Where section size is such that massive cementite is formed, heat treatment will be a must if ductility is desired. About 1 hr at 1650 F and 5 hr at 1275 F seems to be adequate if manganese is kept low.

While it is yet too early to make predictions with any great degree of certainty, we have begun to see a pattern. It is possible by this process to make castings that will have the following properties without heat treatment:

| Tensile, p | osi | 75,000 | to | 110,000 |
|------------|---------------------|--------|----|---------|
| Elongatio | n, per cent | 3 | to | 12 |
| Reduction | n in Area, per cent | 3 | to | 12 |
| Brinell H | lardness | 170 | to | 941 |

For those who subsequently anneal somewhat lower tensiles and greater ductility are to be expected.

Present indications are that its damping capacity will not be as good as cast iron as we now know it. Fatigue properties seem to be quite excellent from results reported by Eagan.³ Impact properties are excellent.

In many types of wear, particularly lubricated wear, investigators are reporting excellent results. There is every reason to believe that corrosion resistance of this material will be equally as good as that of the regular cast iron that we now know.

That time and a great deal of work will broaden the field and create new fields for this material, there can be little doubt. While the economics of the process are now a bit out of line, there is little doubt that this will be brought in line. Alloy costs are much too high

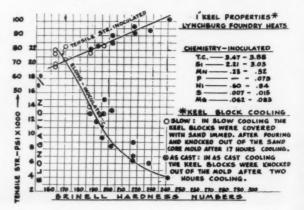


Fig. 8-Graph of tensile-elongation-Brinell relationship in inoculated ductile irons from 1-in. keel bars.

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and, as the demand grows, will no doubt come down.

At the present time it appears to us that this material will be a worthy competitor of cast steel, especially in the heavy equipment field. It should find a large field in plant maintenance castings now made in both cast steel and cast iron. It should find a field in the area of malleables where section size becomes somewhat critical for malleable.

In fields where the lack of any measurable ductility is quite a handicap to the present cast iron, this material should, indeed, prove a welcome addition to the family of cast metals.

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DISCUSSION

Chairman: H. BORNSTEIN, Deere & Co., Moline, Ill. Co-Chairman: T. E. EAGAN, Cooper-Bessemer Corp., Grove

Co-Chairman Eagan: 1 Mr. Kuniansky has presented a very informative and important paper on nodular iron. He has presented a rather detailed discussion of the processing of the material, together with typical examples of the physical properties obtained from actual castings. It is this type of information that is necessary for the proper development of such a new product. Our work on nodular iron confirms to a large extent the information given in the paper.

In order to properly use a material, it is necessary that we have a rather complete story on the physical properties we may expect from it, and also its foundry characteristics. Such information should be a compilation of the results obtained from a number of producers of the material. At the present time we are in need of more information than is now available.

The significance of ductility to a designer has been in question many times. It cannot be used in any of the standard equations used for stress calculation: therefore, why specify it? While this is true, we have found that it has considerable significance in the structural durability of the part when submitted to dynamic stressing. We have found that a minimum of elongation of 5 per cent doubled the fatigue limit and the maximum pressure for bursting of an experimental vessel designed for such testing. It appears therefore, that ductility has considerable significance.

I would like to emphasize one other thing Mr. Kuniansky brought out in his paper, namely, that nodular iron is not one that can be made without rigid control of foundry practice, especially melting. There are a number of foundries that are contemplating going into it, believing that it will solve all their problems. It would be much more advantageous to them if they would improve their ability to make good old gray iron.

F. P. WURSCHER: 8 It was stated that there was a variation in elongation in the as-cast bars and those machined from a casting. Was a metallographic study made of the casting that might have shown a mixed condition of flake and nodular graphite?

MR. KUNIANSKY: My concern with the variation in elongation between that obtained in a test bar machined from a keel block and that in a bar machined from a casting has been occasioned by the fact that we have tried to arrive at some degree of correlation between these two methods of testing. I might add that we have found similar differences in properties between test bars machined from keel blocks poured in cast steel and test bars made from the cast steel casting itself, both poured from the same metal. However, this analogy should not stop us from trying to arrive at some type of test bar on ductile cast iron that will give us reasonable correlation. We have had no difficulty in producing the nodular or spherulitic structure. One of our major problems has been the matter of migration of the nodules and when we get into that type of trouble the ductility almost entirely disappears. Where our melting practice and the practice of adding alloying elements have been under close control, we have no trouble reproducing the nodular structure. In

the examples of pipes shown we have been gratified in the bursting tests of quite a large number of various sizes of pipe. We struck a rather uniform level on bursting, but there again we were dealing with a casting which was subsequently annealed, and in the as-cast condition had a white structure.

J. L. YARNE: 8 Mr. Kuniansky stated that much development work is necessary before we get the kind of product we would like to have. In our experimental work, we were more successful with magnesium-ferrosilicon alloys than with any other type of magnesium alloys. However, the magnesium-ferrosilicon alloy has been taken off the market. You use the nickel-magnesium alloy. Why has the magnesium-ferrosilicon alloy been discontinued? It seemed to be easier to handle than the copper-magnesium or the nickel-magnesium alloy. At any rate, I obtained more consistent results with magnesium-ferrosilicon alloys.

Mr. Kuniansky: We had an opposite experience. We find that the 20 per cent magnesium alloy handled better for us than the magnesium-ferrosilicon alloys. I do not know what became of them. I might add that in treating an iron of very high temperature we have to date had not as good results with the magnesium-ferrosilicon alloys as we had with a 20 per cent magnesium-nickel alloy. We do not know whether that is a problem of density or not, but in the lower temperature work pouring at roughly 2450 F, we have had pretty good results.

J. E. Rehder: On this question of magnesium-ferrosilicon,

some time ago I was not having good results with that particular alloy myself. It seemed to be irregular in results, but when it did work, it was very satisfactory. The suppliers of the alloy apparently have made some improvements of some sort and I am not finding results more reproducible. I have had no diffi-

culty obtaining supplies of the alloy.

With respect to the point about why some nodular irons apparently under good control will have lower elongation than similar irons, I have been of the opinion for some time that the question of feeding nodular iron castings has been underemphasized. I think there has been a tendency to suggest that the feeding and risering necessary for nodular iron is little more than that needed for gray iron, and in my opinion that is not so. I find that in order to consistently get sound castings, nodular iron has to be adequately fed. If the casting is not well fed, you may see no visible shrinkage, but under the microscope, if you look carefully and if you do a good job of polishing the metallographic specimen, you may find considerable microshrinkage. I always over-feed a new nodular iron casting and then cut back on the feeding to the point where mechanical properties start to suffer because of microshrinkage. I would like to ask Mr. Kuniansky if he has noticed anything of that nature.

Mr. Kuniansky: We do not attribute it to the same theory that you describe. We would go along with you on the idea that the problem of feeding has definitely been underemphasized, and I think that if all of the castings shown here were as well fed as that part of it is, we would have gotten a uniform

level of ductility. That is the problem.

S. M. Norwood: 5 The "addition alloys" for producing ductile iron are still very much in the experimental stage, and in the case of the magnesium-ferrosilicon alloy mentioned, there have been definite improvements made. In the very near future you will be able to buy an improved magnesium-ferrosilicon addition alloy which we have found gives castings better ductility in the as-cast condition than did the previous alloy which we had on the market. Five per cent elongation has been obtained with cupolamelted iron in the as-cast condition, and much better elongation has been obtained in iron produced in the high frequency furnace. Production of these addition alloys was held up for several months pending a study of ways and means of controlling the quality of the product. While a number of these problems have been overcome, there is still a long way to go. Further development work on the use of the alloy will have to be done by those who produce the castings rather than by the alloy producers because of the definite limitations imposed in translating results from test bars, as pointed out in Mr. Kuniansky's paper. Within the very near future, however, I believe that the alloys which you thought had been taken off the market will be available to you in a somewhat improved form.

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MANAGEMENT VIEWS COSTS

By J. A. Wagner*

PRODUCING CASTINGS AT LOWER COSTS is of utmost importance to the foundry industry. We must determine what elements comprise the expenses of performing the various operations through which a casting passes in its process of manufacture. We should also devise a plan to accumulate and record these expenses. We will need a means of grouping and distributing them in order to find the cost of an operation or related series of them. Further, a control of expenses is needed that costs of today will be reproduced tomorrow, so that estimated costs can be realized in actual production, during such times when wage rates and material and supplies remained relatively constant in price. These expenditures will be accumulated in three groups-"Productive Departments," these are the departments in which castings are processed-"Service Departments," the functions of these are to perform services for the productive departments-thirdly, "General Cost Divisions."

Cost Factors

The word "cost" as used in our industry and pertaining to a department or an operation thereof should include Direct Labor, Indirect Labor, Labor Extras, Department Supervision and Clerical and Supplies used—including power, either direct or allocated. Additionally, the services rendered a department by other departments of the plant must be allocated to it as well as charges from the General Cost Division.

The manner of accumulating these costs and the specific distribution of them are best described in the 1949 Cost Accounting Manual of the Malleable Industry. It is suggested that the procedures advocated in this manual be used. They are equally applicable to all branches of the foundry industry. When labor in a department or an operation is wholly or in part displaced by machinery new and off-setting expenses are inherited such as mechanical maintenance, power used and cost of depreciation of the equipment. An accounting system which does not record all these

elements in the cost of the operation or department would fall short in giving a measurement of the true cost—the importance of this cannot be over-emphasized.

The job of controlling costs is relatively more complex than that of summarizing them as only a few general conditions can be set up-the balance are dictated by plant and personnel peculiarities and differ widely under local policies and conditions. However, to start with, a sound policy for setting piece rates or the establishment of output quotas is a necessity. Without it direct labor costs cannot be individually relied upon. A well set-up job rating system is a prerequisite for evaluation of all classes of labor, especially when various operations are placed on incentive basis. The indirect labor as such is almost unpredictable in a jobbing foundry, and the control of it, if on the basis of tons or hours of labor per ton, etc., fall short in reliability. This from the standpoint that many such plans do not reflect the differences in cost of different kinds of work passing through the particular department.

Perhaps the best approach would be to attempt classification of product into a number of general groups. Reasonably accurate time allowances can be arrived at by timestudy for each of these classes or groups. This at least would give supervision a reasonable yardstick for measurement of labor performance in this operation and if labor could then be shifted to and from other operations, as the work load varied, the indirect labor could be considered under reasonably good control. Of course, if management does not demand and receive a fair labor output, the effects would be largely destroyed.

The term "labor extras" is intended to include the payment for overtime premiums, shift differential, delay time and allowances and by the nature of the accounts it is indicated that this group of expenses lends itself to controls provided the expenses are properly assembled and recorded. It is advocated that these be associated with each cost center, instead of being accumulated in one total account, as perhaps in an overhead category. The figures appearing in the expenses of the cost center are of much greater value than if the same dollars were included in one

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[•] President, Wagner Malleable Iron Co., Decatur, Ill.

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account which represented the totals for all departments.

General supplies used in each of the cost centers are likely not controllable unless their usage varies directly with the activity of the operation or the cost center. However, if the supervisors are made acquainted with the cost of supplies under their immediate direction they can be expected to take steps to assure the proper and efficient use of them. It is advocated also that in each cost center a separate account be provided for the recording of purchases of machinery repair parts which are made for equipment in this activity. The control on such an expenditure is best, I believe, when there is knowledge of the amount of expenses and then in keeping with the importance of them, a standard is set up for ap-

propriate preventive maintenance. The accounts and practices discussed are applicable to the cost centers in both the productive and service departments. Beyond these we also have certain accounts in the general cost divisions or overhead accounts which are susceptible to some control. Obviously less can be done in this category as our first attempt has been made to channel as many items of expense as possible into productive and service departments leaving only a minimum of accounts in the overhead group. These are largely things like depreciation of both building and equipment, certain taxes, salaries for general supervision and clerical and wages for watchmen, etc. Additionally, of course, there are certain associated expenses which we caption "Expenses Related to Payroll." If specific accounts are provided for the various expense items in this group an impressive total will be found and one which is worthy of study because of its relative high rate of cost. This account likely also will require some reserves because certain expenses like vacations, etc., must either be anticipated or in cost finding must be absorbed in terms of yearly expenditures. Taking this group, together with the previously discussed "labor extras," and relating that total expense to the payroll of a cost center, it is not unlikely that a dollar of wages in many instances results in a labor cost of \$1.25 or more.

Machinery Depreciation

The item of machinery depreciation contained in the general account is controllable by management because it is a theoretical figure. Expenses recorded in the general books usually have little relationship to costs as represented by current rates of production. For cost evaluation or cost finding purposes it is recommended that inasmuch as a piece of machinery displaces labor and that we have previously advocated taking the power and maintenance expense to the cost of the operation or the cost center that we likewise apply the machinery depreciation to such costs in proportion to the rate of activity. Saying it differently, it seems desirable that depreciation cost should vary directly with production of the operation of which it is a part and that the basis of the cost should be consistent with the evaluation placed on the remaining life of the asset. It could also be argued that the present replacement cost rather than

the original cost should be considered when setting up such a rate. If for cost finding purposes some such approach is acceptable then it follows that machinery depreciation becomes a direct cost in a cost center as it would vary directly with production and on the basis that under normal rates of activity a fund would be reserved or created which over a period of time would be sufficient to purchase replacement equipment. The remaining accounts in general overhead can in cost finding, be considered to be grouped and applied to each cost center in proportion to the recorded expenses at a rate which again at normal activity level would absorb the expenses which would be anticipated to support such production. This general group of expenses is not controllable as such, but could be made to vary under budgetary control within reasonable elements.

The words "normal" and "normalization" have been used in this paper and are generally accepted terms in the industry. However, opinions of its use and even of its meaning vary considerably and it is not intended in this paper to give specific recommendations on the subject other than to say that certain costs would be meaningless when used to determine the cost of an operation or a casting unless they were normalized. The term should not, however, be considered a means toward cost reduction but rather in policy it should create a leveling of costs. The continued misuse of normalization or an ill-advised basis for the application of the theory will ultimately become only the interest of the local sheriff.

Overhead Account

In our discussion there remains only the overhead account usually referred to as general administration and selling expenses. These expenses are not considered when we analyze the relative cost of performing this or that operation under different methods and comes into our problem only in cost finding of an order of castings for which reason we will make no further comments excepting to stress the importance of a proper conception of normalization when using the account in "Cost Finding."

Our theme is "Making Castings at Cheaper Costs." We do not mean at a sacrifice in quality or in profit either. Rather we must accomplish this by efficiency which takes us to the study of methods. I think we create new ways of performing certain operations and the handling of materials not only to achieve lower costs but also, and very importantly, to improve working conditions. I do not think the urge to do these things is necessarily prompted entirely by competitive conditions within our respective divisions of the foundry industry-instead, I feel that the desire to expand our sales horizon into fields supplied by competing materials is very compelling. The greatest challenge which could be given our division for greater and better forward strides in methods would be the switch from malleable to another material of some certain part presently produced in large tonnages. Further, I believe our interest is, or should be, to operate institutions which can hold what work we now have, against the inroads of competing ma-

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terials and also to reach into those fields to attract

some of its products to our plants.

None of us can see the sense of losing money on a contract just for the satisfaction of accomplishing a "conversion." Conversely it seems useless to resist when a competing material is offered at prices which, if met, would result in losses to us. A lazy attitude toward knowing costs when the profits and penalties are potentially so great seems inexcusable. During the same period our competition is viewing our sales field with envious eyes and whether we like it or not we are constantly engaged in a tug of war. I believe each of us is sincerely anxious to produce a quality product and also that we have a high respect for the good will enjoyed by our individual plants. However, regardless of our high esteem for castings generally, we can not close our eyes to the certainty, if quality is comparable, that the material representing the lowest cost will meet with acceptance.

New Markets

The field for new markets is so unlimited that we can not afford to continue doing things today the way they were done yesterday if the costs under the new methods will put us into a better competitive position. Perhaps our slogan should be that the method must be wrong if it has not been changed in recent years. But, again, we must know how much we are spending to do a certain operation before we can determine that a proposed method will give us a lower cost and better working conditions. Obviously we must also know the present cost of performing all of the operations so that we can compute the cost of production of a job or an order of castings through the complete process of the plant. It seems academic that managers in their search for profits and new sales should spearhead cost analysis. Instead, the responsibility is frequently passed to subordinates, who in the majority of instances have only remote connection with decisions on policies of sales or the purchase of new equipment.

We have talked about costs and a means of accumulation-now let us consider what we can accomplish with such records and knowledge. Perhaps an example will be helpful. Let us assume a typical jobbing foundry which has used the principles of cost accounting and the procedures for cost finding which have been sketched for you, and in keeping therewith it has a department called "Sand Preparation." Also let us assume that originally sand for molders use was prepared on each floor by means of a shovel and hard work. The cost is now essentially wages and items of expense related to payroll. Burden charges would be confined to the cost of an occasional shovel. Next the manufacturer purchased a mechanical sand cutter which was used on some of the floors, the remainder continued to be served by the shovel method. First, from the accounting viewpoint, it becomes necessary to create on paper a new cost center. In the "Sand Preparation" Department we now have two cost centers, the first of which we entitle "Hand Cutting" and the new one "Mechanical Cutting." Each has accounts for operating labor, labor extras, etc., as previously described. 'The total labor

cost (wages plus allocated "Expenses Related to Payroll") shows a decided saving in favor of the new method.

However, this is not the full story because our accounting system will now automatically record electricity or other power used, repair parts for the machine and in Cost Finding, the center additionally will be charged with expenses allocated from the Mechanical Department and other Service Departments in proportion to services which have been rendered plus charges from the "Expenses Related to Payrolls." If now, in addition to knowing the dollars of direct and allocated costs, we know also the amount of material processed, we can quickly compute the rate of cost in respect to the production factor we have selected. This requires us to maintain certain production figures-in this example it would be molds poured times the sand mold weight. The resultant cost rate may be compared with previous results or it may be useful and interesting as a comparison of the cost of the same operation performed in the other method. The management can now be assumed to have full realization of the rate of cost per ton of sand used under the two methods. The manager now believes better and cheaper sand can be prepared by a more improved method. An estimate is set up pursuant to the accounting principles and procedures he has been using and after due allowances have been made for the cost of repairs and depreciation projected over the expected life of the equipment, he obtains an estimated rate of cost per unit of activity-in this case, tons of sand used. He has a good comparison with the cost of the two methods now in use and if he buys the new equipment, he will have a goal to aim for. Again he would set up a third cost center to record expenses if he invests in this additional and different equipment.

Need for Cost System

This example is given to emphasize the need of a cost system which has principles and procedure applicable to the costing of present as well as new methods of manufacture without the need of a major change in accounting practices. I hope it has also illustrated the practical help it affords in cost controls. When the cost system has been applied to all of the cost centers of the enterprise, we have a series of cost rates by operations, some of which are in terms of tons poured, some per molder hour, etc. Individually they tell the cost story of a particular operation and thus they become the final record of the efficiency or lack of efficiency. The rates, being dissimilar, cannot be added together to give the ultimate cost of a casting. What we are concerned about is an assurance that when we invest in new equipment that our cost methods will properly evaluate these new costs. It is our text that savings in costs should be passed on to users-however, we had best be fortified with a thorough knowledge of these costs lest we pass on to our customers not only savings, but also the cost of our molding systems, hydraulic presses and annealing furnaces. They want none of these-they would much prefer that we be prosperous and use profits for the continued purchase of constantly imOSTS

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proved equipment to further lower costs.

Up to this point we discussed the comparative cost of preparing sand, of making a mold or of melting in this method or that. Now let us turn our attention to the costing of an order of castings. We start with the premise that management is as sincere in wanting an adequate cost system as in wishing to make a fine product. If he does not champion the cause of good costs he likely will fail in business either because he quotes prices below his costs or because his costs are so high that he excludes his plant from the market. Many institutions are continuing to fill the needs of casting buyers because their facilities are fully depreciated. Likely also their bank accounts are depreciated. If that be the case it can hardly be expected that they can or will contribute to the program of "Making Castings at Lower Costs."

The key person in controlling casting cost is the Chief Executive. The results are apt to be in direct proportion to his interest. The costing of a casting is not confined to the activity of the Cost Department or a cost clerk. Imagine what happens when an inquiry is received—an estimated cost is prepared, a quotation is made and an order follows. Subsequently, production planning, shop operations, shipment and finally the day of reckoning when somebody wants to know what has happened to the profit we expected to get on the order. Office practices differ depending upon size of plant and types of personnel.

In the case of a medium-sized foundry I believe the Engineering or Sales Department would first check design, sections, etc., to determine that as drawn the part was practical and could be produced economically. If not, the print would be returned to the buyer with recommendations. Next, the inquiry would receive attention of the Timestudy and Methods Department or some department competent to determine what path the part would take through the plant, the rates of pay which would be established for piece workers and rates of output which might be expected in certain operations-the unit weight, as-cast and final-the pattern equipment, design and cost, the total metal and sand weight of the mold, rates of scrap, the determination about cores and also the need of this or that finishing operation. Next, the order would go to the Cost Department where the clerical work of preparing the cost card would be carried on. The cost formula has previously been prepared from the actual operating costs of the previous quarter and with the use of Standard rates for the absorption of overhead charges. To reduce clerical time the cost of those operations (not including direct charges) which have the same distribution factor are grouped together and expressed as rates of labor cost and of burden cost.

Cost Card

The Cost Card which reaches the Sales Department, is so arranged that all Direct and Indirect Labor is itemized in one column and burden charges in another—variable overhead, payroll expenses, materials each are recorded as single entries. This form gives opportunity to re-evaluate the impact of a wage change, etc. To determine the rate of cost, the Sales

Department must predict the number of pieces or pounds which have been costed which will be paid for by the customer. The Sales and General Administration expense (normalized rate) has been applied as a function of the Conversion Cost-the intended profit margin should be applied on the same basis. The Sales Engineer now has a potential customer and a cost card. What happens when the buyer intimates that the price is high? Is he able to convince the Purchasing Agent that the price is equitable for the services to be rendered and that the facilities and personnel of his foundry will assure good performance? He must be sold on the efficient management of his company-the production of the product as well as the thorough accounting of its costs and the careful and intelligent preparation of the probable cost of the part in question. If he has this feeling I am sure that he would have confidence that the projected cost figures were reliable-on the contrary he might be suspicious that the card he has been given represents a lot of guesses. In such case he will be an easy target for a buyer whose only interest is first cost. Certainly the chief executive is responsible for the proper function of these various people or departments and just as certainly it is apparent that "Costs" are basic in each of these activities.

Regardless of modern equipment or the high plane of personnel that the Company has, that it can succeed only if it has profitable sales. Surely it is Management which is charged with the responsibility of getting profits, and just as surely it is a fact that you cannot consistently make profits unless costs are known and hence that Costs are a prime business of Top Management in the profitable production of quality castings at constantly lowered costs under improved methods and working conditions.

DISCUSSION

Chairman: R. L. LEE, Grede Foundries, Inc., Milwaukee.

Co-Chairman: G. TISDALE, Zenith Foundry Co., Milwaukee. T. R. WALKER: My one concern is to get cost figures more rapidly. We are having a consultant to help us do this. How do you go about getting in cost figures before the 12th or 15th of the month when your production figures are not available until the fifth, sixth or seventh operating day?

MR. WAGNER: I wish I knew. I think the only answer is control of ccsts. You know pretty well what the figures are going to be, but I do not see how you can say what a cost is until all the figures are assembled.

MR. WALKER: In other words you use an estimate.

MR. WAGNER: An estimate is worthless unless it is carefully and intelligently prepared. Then you constantly compare the estimates with actual cost figures.

Are you interested in these figures for estimating new jobs or are you interested to know what your operating practice was the previous month?

MR. WALKER: I am interested primarily in knowing how good our performance was the previous month.

A. E. HAGEBOECK: 2 You are interested in the operating procedure. If you are getting those figures by the fifteenth of the month I do not think it is so bad. We pride ourselves because we get it around that date.

CHAIRMAN LEE: One of the things the author tried to emphasize is the fact that this estimate becomes a standard until it is proven wrong. That, in a sense, is what you do.

MR. HAGEBOECK: Yes, your estimated cost becomes the standard until it is disproven.

¹ Vice President, Warren Foundry & Pipe Corp., Phillipsburg, N. J.

² Executive Vice President, Frank Foundries Corp., Moline, Ill.

CHAIRMAN LEE: What we do and one can do it without much trouble is not to pay any attention to anything that is standard. If it is standard, then you are making your proper profit or you have your proper cost, assuming, of course, that your estimate is fairly accurate. It is those things which for some reason are either higher or lower than the standard which you have to set up. They are the things that cause the trouble. Therefore, if you investigate and account for those, you do not have to wait until the twelfth or fifteenth of the month when you receive a report that will state those things. Those things are showing up every day as they occur.

MEMBER: We use cost figures based on our experience the preceding quarter. We get our direct and man-hour figures every day. It is our material cost that causes trouble and we do not get those until the fifteenth. We get lax in those figures and then before long they are high and our costs are high.

I would like to get some information on how foundrymen can get their material cost from day to day so that their operating

department will know what is going on.

MR. WAGNER: It is a matter of the definition of "material." In our estimation materials are those things which are melted down and which become an integral part of the casting which we ship as differentiated from "supplies" which would be coke,

gas, oil, sand and stone, etc.

MEMBER: You use a known element of cost because it is charged each month, not at the cost of the inventory but at the going market, so you must be observant of the trends of the market and know your material and supply costs. You cannot predict on that basis. They will vary in production. In other words, supplies will not vary directly with production. They will vary directly with quality rather than quantity of production.

MR. WALKER: Is it customary, in the industry, to roll inventories on raw materials monthly, to take your inventory the first of the month, add in your purchases during the month and determine an average cost of the metal for the monthly accounting? We roll inventories on raw materials, supplies and production.

CHAIRMAN LEE: It depends on a rising or falling market. What was said about inventory and supplies on the first of the month at the market is what any wise foundry, in my opinion, would do. Of course, that is true regardless of whether it is a rising or falling market, because if your market is falling your market inventory price is going to be less, but it is going to make you realize that the market is falling.

MR. WAGNER: We are a bit confused between cost accounting and cost finding. When I discussed the market as being the basis for material costs, I should have said that it is for cost finding purposes. Our general books, or accounting books are carried on the basis of our costs which go back to our inventories. The same thing might be true also of our overhead expenses and

machinery depreciation.

CHAIRMAN LEE: I think what you meant by depreciation holds true there and it certainly is true in what you do for income tax purposes, where you are limited in depreciation or otherwise for accounting at the end of the year has nothing to do with what you are going to do for cost finding purposes for current opera-

You not only cannot compare today's production with that of last month's but you cannot even compare it with yesterday's. I have never seen a foundry that produced the same casting under the same conditions on two consecutive days. You must establish standards so that you can judge what is being done day by day, today's production today, at today's prices.

MR. HAGEBOECK: I enjoyed Mr. Wagner's paper very much and sincerely wish that we could get more men of top management interested in costs. All managers are interested in costs, but so few will give a Cost Program their backing, and as a result the man in charge of costs has difficulty in carrying out an adequate cost procedure. We all realize that no proposition in a foundry has a chance of succeeding unless top management backs the program 100 per cent.

MR. WAGNER: The Foundry Industry made considerable progress in the last few years but there is much additional work to be done in developing new methods and producing at lower costs. We must determine the cost of operation with today's methods and set up procedures so that we will know what the cost will be tomorrow with improved methods. By that I mean if the new equipment produces at the cost anticipated. To do that you will need accounting procedures and cost controls. The top man in the organization will have to take a personal interest in the matter.

CHAIRMAN LEE: That is precisely so. It also seems to me that in the last few years foundries have become dormant. Their plans have grown dull through lack of use. They must find methods of making better castings at lower cost. That is the only salvation of the Foundry Industry in the future.

MEMBER: Suppose you ship 1000 castings to a customer. The customer rejects 200 of these castings and returns them to you as scrap. Is this scrap treated as an overhead expense, a manu-

facturing expense or a direct cost item?

MR. WAGNER: We apply it to the customer's account as a whole. We carry a quarterly profit and loss for each customer. It would be applied as a sales deduction for that particular customer. Unfortunately it might not be charged against the same profit and loss statement which covered the cost of those 200

CHAIRMAN LEE: Do you not budget and make a provision for

return of those castings at a later date?

MR. WAGNER: We have taken that into account in the mark-up above cost. Costs are computed in the usual way but in the case of certain kinds of castings, which have to be sold subject to pressure testing, or some other form of special inspection, where the returns are expected to be greater than in ordinary cases, then we have an addition in our profit mark-up to arrive at the proper selling price.

CHAIRMAN LEE: There is a sort of fallacy there. If you can market those castings over a period of say four months you have added that extra cost on each of those four months and have accounted for that as a profit. Then if they all come back in one month, you will have quite a reduction in sale and a rather sharp

reduction in profit.

Mr. WAGNER: Is it not better in the case of a pressure casting where the customers return is four or five times greater than ordinarily to provide during those production months a reserve to take up that sharp reduction of sales at the time the return occurs? We can accomplish it because we carry our historical costs on an accumulative basis on every job.

CHAIRMAN LEE: You would in that respect.

L. D. Sacks:

On depreciation of machinery, according to activity, how much of a change in activity would be desirable

before you change your function?

MR. WAGNER: In the case of a mold conveyor it is first determined what the value of the equipment is as of the beginning of the accounting year. Then we would determine the number of molding hours that the machine could potentially be used. This would be the number of stations times 2000. We would next decide what would be "normal" activity for that molding conveyor. Let us say that normal would be 70 per cent, as an example. Instead of using 14 stations times 2000 hr of molding time we would use 1400 hr times the number of stations. Then those hours divided into the amount of money determined to be the correct annual depreciation on the value would be the rate of depreciation. Each month's cost in cost finding would be charged with that rate times the number of molding hours that that particular molding conveyor had been used.

Mr. WAGNER: Proper engineering of a cost estimate on a particular casting has not been emphasized in this discussion. Your timestudy, engineering and standards departments have a very important function in the original preparation of the final cost figures. If these departments do not make their estimates correctly then your cost figures will be out of line. Factors involving in arriving at a proper cost figure are the various methods of producing the castings, design, weight, potentiality of scrap in process, etc. However the most important time in estimating cost is the time you yourself spend with the blueprints before passing them on to the others for consideration.

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⁸ Supervisor, Sacks-Barlow Foundries, Inc., Newark, N. J.

EQUIPMENT FOR DEGASSING MAGNESIUM ALLOY MELTS

By
Alex J. Juroff*

IT HAS BEEN KNOWN for some time that the presence of dissolved gases in magnesium alloy melts is a strong contributory factor toward the formation of microporosity. Recent investigations by DeHaven, Davis, and Eastwood¹ as well as, Busk, Marande, and Newhams² have shown quite conclusively that hydrogen is the principal offender. These investigations have also shown that the removal of dissolved hydrogen with chlorine provides a reliable method of gas control. Eastwood, Davis, and DeHaven³ discuss the mechanism of the chlorine treatment rather thoroughly.

Specific references to the equipment used in the degassing operation are rather brief, however. Eselgroth⁴ has described apparatus used for gas reduction in aluminum and copper alloys using nitrogen as the flushing medium. The applicability of the Eselgroth equipment to magnesium alloys was not reported.

Degassing Apparatus Described

The purpose of this paper is to describe the constructional details and operation of suitable apparatus for performing the degassing operation on magnesium alloy foundry melts. The apparatus herein described was designed and developed for production operation by production personnel using conventional melting equipment.

Basic features of the equipment are shown in Fig. 2. The chlorine cylinder is rigidly mounted in a steel frame which rides on rubber tired casters as shown in Fig. 1. Suitable hangers are provided for the tube assembly when the unit is not in use. This arrangement provides a mobile unit that may serve as many superheating furnaces as the melting cycle permits.

Cylinder Clamp, Adapter Recommended

Referring to Fig 2, the auxiliary tank valve, 3, may be joined directly to the chlorine cylinder. However, the use of a cylinder clamp and adapter is recommended.⁵ The needle valve, 5, should be secured to the framework by a clamp. This valve is used to make fine adjustments in gas flow while the unit is in operation. The hose, 7, may vary in length to suit the operation, five to seven feet generally being sufficient. The lining of the hose must be neoprene, and double braid construction is preferred. The use of a quick opening valve, 8, is optional—satisfactory operation can be obtained without it.

Since the pipe, 9, carries the load of the graphite tube assembly, it should be of either heavy or extra heavy construction. It is advisable to provide some

means of supporting the tube assembly while the unit is in operation. This may be done by welding a length of steel rod to the pipe, 9, as shown in Fig. 2. This rod may then be drilled with a series of holes along its length. To place the unit in operating position, the rod is slipped into a tube welded to the furnace shell. Depth of the graphite tube assembly may then be adjusted by using a pin through the proper hole in the rod as shown in Fig. 1. The graphite tube should extend approximately two-thirds of the distance from the top of the melt to the bottom of the crucible.

For optimum results, the degassing operation should



Fig. 1—Adjustable bracket held by pin enables graphite tube carrying chlorine to be adjusted to proper depth.

be performed with the metal at a temperature of 1330 F to 1360 F. Below 1330 F, magnesium chloride is slow to form with the result that excessive chlorine escapes into the atmosphere. Burning on the melt surface tends to be excessive at temperatures below 1330 F. Apparently the magnesium chloride dissolves sufficient oxide to raise its melting point which reduces its fluidity and consequently its covering ability. Operation at temperatures in excess of 1360 F results in shorter tube life and in the formation of larger amounts of magnesium chloride.

Before degassing a melt, the graphite tube assembly should be thoroughly preheated. Valves 3 and 5 should

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Foundry Metallurgist, Bendix Products Division, Bendix Aviation Corp., South Bend, Ind.

then be cracked and the tube positioned over the melt. Valve 1A may then be opened and at the first sign of the yellow-green fumes at the tube tip the assembly is slipped into operating position. Coarse adjustments in gas flow may be made with valve 3 and fine adjustments with valve 5. Once adjusted these valves may be left in a set position. The chlorine flow should be adjusted to give a gentle boil to the melt with the bubbles approximately two inches high.

Care In Melting Cited

Care should be exercised that metal is not spilled over the sides of the crucible into the crucible setting. Five to eight minutes of chlorine treatment should be sufficient for a melt comprised of a clean dry charge Questionable or contaminated melts should be treated from 15 to 20 minutes. At the end of the degassing period, valve 1A is closed and the tube quickly removed from the melt. With training and experience a single operator can perform the operation with ease.

Observations And Conclusions

Most frequent failures occur in the graphite tube assembly at the metal line. Tube life varies from 30 to 75 heats depending on usage, construction, care, and other variables. Hose life of 1000 heats is not uncommon if the hose is neoprene lined. Ordinary rubber will fail after two or three applications due to chlorination of the rubber.

On the basis of a seven minute cycle using a 130 lb capacity magnesium crucible, approximately 0.04 lb of chlorine per pound of metal is consumed. The metal loss due to the formation of MgCl2 has not been

quantitatively determined. Assuming, however, that the reaction

 $Mg + Cl_2 = MgCl_2$

is 100 per cent complete, then, on the basis of 0.04 lb chlorine per pound of magnesium alloy, a maximum loss of 1.3 per cent may be expected.

Experience with the apparatus described has led the

writer to the following conclusions:

1. Treatment of magnesium alloy melts in the foundry with chlorine supplements but does not replace good gating practice.

2. Properly executed, the chlorine treatment provides metal of more consistent fluidity from heat to heat. The variation in pouring time for a given mold is considerably reduced.

3. The operation is easier to perform and lends itself to better control than the older stirring method of refining.

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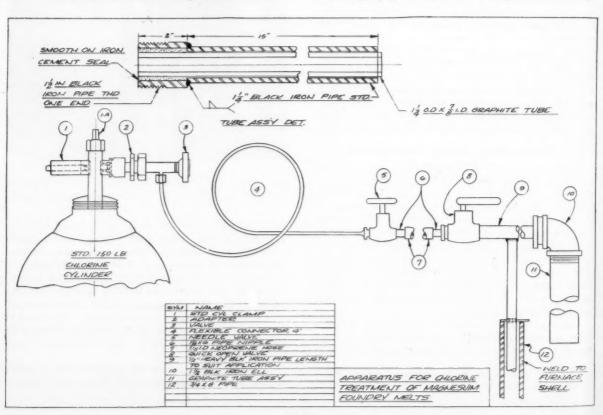
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Fig. 2-Construction details of simple, effective chlorine degassing equipment for use in magnesium foundry.



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DISCUSSION

- Chairman: M. E. BROOKS, The Dow Chemical Co., Bay City,
- Co-Chairman: E. J. VARGO, Wellman Bronze & Aluminum Co., Cleveland.
- H. C. CHADEN: 1 Is there any danger in working with an alloy in an open type furnace? It might not be brick lined and the chlorine is close to the open metal. It might get too hot.
- MR. JUROFF: By extending the length of the flexible tubing, the chlorine cylinder may be kept away from the furnace heat.
- MR. CHADEN: When you enter the stream of gas into the molten metal, is the volume of the stream a factor in successful operation? Must you use a small orifice and a fine spray?
- Mr. Juroff: The dimensions of the graphite tube are given in Fig. 2. Although we use a 7/8-in. ID tube, many operators prefer the 1/2-in. ID tube. Either seems to function satisfactorily. So far as we were able to determine, the size of the opening is not too critical.
- J. W. Meier: 3 These were some serious failures because proper valves and connecting tubes were not used. It is very important to use cadmium-plated needle valves and connecting tubes to avoid excessive corresion from chlorine. Ordinary brass valves with plastic compound valve seats or rubber diaphragms are unsuitable and may cause trouble.
- MEMBER: As a further comment on this question of heating the tank of chlorine or getting it too close to the furnace, I believe most suppliers of chlorine include a fuse plug in their tanks which, if the cylinder becomes too hot, will melt and the chlorine will escape and relieve the pressure somewhat. I am not sure all of them do, but some do.
- Mr. JUROFF: Yes, that is very true, and I believe it melts in the neighborhood of 160 F.
- JOHN MEZOFF: 8 To what do you attribute the more consistent fluidity in treated metal as contrasted to metal treated in other ways?
- Mr. Juroff: Although we do not have experimental data to substantiate our opinion, we feel that the improved fluidity of magnesium-alloy melts treated with chlorine is due to more complete removal of suspended oxide particles from the melt. We feel that the magnesium chloride which is formed during treatment dissolves or entrains the oxide particles and brings them to

- the melt surface.
 - Mr. Mezoff: Do you use screens in your practice? Mr. Juroff: Yes, we do.
- Mr. Mezoff: In other words, you believe that the screen in functioning to gather up these nonmetallics is what appears to be a more sluggish nature of the metal when not treated.
- Mr. Juroff: Apparently that is it. However, we believe that high hydrogen content of the metal also has a detrimental effect
- K. E. NELSON: 4 We agree with Mr. Juroff in his observation of more consistent pouring rates obtained using chlorine gas to refine molten magnesium alloy. I would like to know how the author puts in 0.04 lb of chlorine to a pound of metal in 5 to 10 min. It is my experience that bubbling chlorine into molten magnesium at that rate is going to throw a lot of metal outside of the pot. I wondered whether the author's crucible is differently designed from what we normally use.
- Mr. Juroff: The 0.04 lb of chlorine per pound of metal figure was calculated using data collected from the production floor. Mr. Nelson is quite correct in questioning this figure since in production the degassing times may vary from 5 to 20 min. Therefore, the actual consumption is probably more on the order
- of 0.02 lb of chlorine per pound of metal. MR. MEIER: The author mentioned burning at the melt surface at temperatures below 1330 F. We did not observe any burning but found that at these lower temperatures a good exhaust system is very essential to avoid difficulties due to excessive chlorine escaping from the melt. Chlorination at temperatures from 1330 to 1360 F is much more efficient and safer. In any case, the necessity of proper ventilation of the melting installation wherever chlorination is used, should be stressed.
- Mr. Juroff: When degassing is done below 1330 F, the surface of the melt takes on a flaky appearance and burning on the surface is excessive. This, we feel, is due to the decreased fluidity of the magnesium chloride which entrains particles of magnesium alloy and exposes them to the atmosphere.

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AN ALLOY DESIGNED FOR PATTERN SHOPS

By

S. Zuckor*

ABSTRACT

The author discusses a copper-silicon-aluminum-magnesium alloy for metal pattern applications. He discusses its properties and melting and pouring practice in the foundry to attain optimum results.

OVER A PERIOD OF SEVERAL YEARS we have been requested to develop an alloy that would serve as an all-purpose alloy for the pattern shop. We feel there is no alloy that will cover every single application in every shop, but we have designed an alloy that will cover the majority of pattern castings made in most shops.

The alloy most commonly used for pattern work is the copper-aluminum alloy. This alloy, one of the first in use by the foundries, has been used for years and has not been changed because the patternmaker purchases such a small percentage of the aluminum casting alloys used in the country, making expensive research impractical. The difficulties with the copperaluminum alloys are:

1. It is hot-short, and

2. Its shrinkage behavior is not too desirable to the patternmaker.

The writer's company, knowing what the patternmaker most desired, studied the following combination alloys:

> Copper-Aluminum Alloys Copper-Silicon-Aluminum Alloys Copper-Silicon-Aluminum-Magnesium

and we found by varying the amounts of copper, silicon, aluminum and magnesium we had developed an alloy which would be economical and result in sound castings. Our alloy contains copper, silicon, aluminum and magnesium in addition to grain refiners such as titanium, zirconium, boron and beryllium.

Aluminum alloys containing silicon exhibit the following properties which are good for the pattern shop:

1. The castability of the metal is at its best when

silicon is present. In other words, the subject alloy has excellent fluidity and with this added fluidity you are able to pour thin and intricate sections.

2. Its resistance to corrosion is excellent. This is proven by the fact of the many applications in ship parts and other marine uses.

3. The aluminum-silicon alloys are less hot-short than the aluminum-copper alloys and the shrinkage, though perhaps present, is localized and is not the spongy mass that results from use of copper-aluminum alloys.

4. The subject alloy can be melted and handled at low temperatures if necessary.

5. Another important factor is machinability. Aluminum-silicon alloys, as such, are not as easy to machine as the copper alloys, but we have built our alloy to attempt good machinability in balance with the other favorable properties of the aluminum-siliconcopper combinations. This is accomplished by additions of small amounts of copper and magnesium to the subject alloy.

Properties of Alloy

The subject alloy also has many other good properties such as lustrous appearance, excellent thermal stability, high yield point, high tensile strength and high bearing strength. It is ductile and can be bent and straightened. Its yield point is approximately 20,000 psi and it has a 28,000 psi ultimate strength. The elongation is 3 to $3\frac{1}{2}$ per cent.

Before the patternmaker or the consumer receives from his source of supply an alloy the following qualities should be considered in addition to the chemical

1. The alloy must be sound, free of gas, free of oxides and must be free of nonmetallic inclusions.

2. It should have a small grain size and be properly fluxed so that the castability and strength are at a maximum.

Once the ingot conforms to all these requirements it is ready for use in the foundry.

In developing the subject alloy we did not design an alloy that had to be melted under special conditions and poured under special conditions. We realized that most of the patternmakers in the country did ti d tl

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Chief Metallurgist, Samuel Greenfield Co., Inc., Buffalo, N. Y.

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not have the facilities or extensive foundry equipment and were not equipped for production under conditions of this kind. Therefore, the subject alloy was designed so that it could be melted and poured under the same conditions as any other alloy that had been previously used. The alloy under discussion is one that can be utilized by the average pattern shop.

Melting and Fluxing Procedure

Most difficulties in the foundry exist in the melting procedure. We reiterate what we consider good melting practices. The following recommendations are precautions that may be used in case drosses are encountered during the melting period. All solid metal coming in contact with a liquid bath should be clean, free from corrosion and preheated. This means that ingot metal should not be stored in damp places or on the outside. After the ingot is delivered to the melting floor it should be preheated. If you cannot do this allow it to stand for awhile after melting and you will find the drosses will rise to the top and can be skimmed off.

Salt fluxes with a combination of chlorine are always good in removing oxides from molten metal. If your melting equipment happens to be an iron pot clean it thoroughly and coat the inside of the pot with lime wash before charging metal to the furnace. Dry out the wash before charging metal into the pot.

If you are using a clay or graphite crucible preheat equipment before starting your charge. Melt the ingot as rapidly as possible under slightly oxidizing conditions. One of the simplest tests to see if you have an oxidizing condition is to take a small piece of zinc and place it in the flame. If the zinc has a black coating you are melting under a reducing condition, if the zinc is white you have an oxidizing condition. Be sure that the temperature does not exceed 1350 F. High temperatures will tend to increase the hydrogen gas absorption and the oxidation of the metal. Hydrogen gas is probably one of the most detrimental gases to molten metal. Do not agitate the bath any more than necessary. Do not stir the metal more than necessary, and when stirring is necessary stir only before pouring the molten metal into the mold. Stirring should be done with an iron rabble from the bottom upward taking care not to disturb the surface.

Once the ingot is molten and the temperature of the bath is between 1300 and 1350 F then is the time to flux your metal. In our opinion the best fluxing action is done by chlorine gas but realizing the precautions needed while using this gas we suggest that a commercial flux recommended for cleaning be used. When buying such commercial fluxes question the supplier for the amount to be used to cleanse your bath and we are confident he will give you the proper percentage to be used per 100 lb of molten metal.

After fluxing the metal allow it to stand for 5 to 15 min. This will permit oxides to rise and then be easily skimmed off the surface. We cannot emphasize this point too much since many foundries are inclined to pour their metal just as soon as it is molten. This is not good foundry practice. The most satisfactory pouring temperature is approximately 1250 to 1300 F.

Molding Sand Properties

We find when pouring the subject alloy into a green sand mold the following conditions should be present:

- Permeability of the sand should be approximately 31.
- 2. Moisture content of the molding sand should be 5.5 per cent.
- Compressive strength of the sand should be 3.8 psi.
- 4. Temperature of the sand should be 72 F.

We realize that many pattern shops do not have the proper sand equipment and do not test their sand. If you are making any quantity of patterns we advise you to obtain some type of sand testing equipment that will indicate the moisture of your molding sand. The alloy may be susceptible to some gassing if the sand does not contain the proper moisture content. When pouring the metal from the crucible to the ladle maintain a steady, uniform stream. This is good practice in handling all aluminum alloys. When pouring your casting maintain this same short uniform stream. If you take these precautions you will be assured of attaining the maximum qualities from the subject alloy.

By introducing this alloy we feel we have taken a step to minimize the patternmakers casting problem without cluttering his shop with numerous alloys.

In closing, we wish to state that the metal pattern is only as good as the metal from which it was produced.

DISCUSSION

Chairman: G. E. GARVEY, City Pattern & Foundry Co., South Bend. Ind.

Co-Chairman: H. C. SWANSON, Arrow Pattern & Foundry Co., Chicago.

D. L. Colwell (Written Discussion): 1 I was glad to see Mr. Zuckor's comments on melting, fluxing and handling of aluminum and hope they are taken seriously. In many instances the metal receives the blame for improper fluxing, melting or other handling in the foundry.

The subject of the paper, however, covers a specific alloy and nothing specific is given concerning that alloy. In presenting the paper Mr. Zuckor mentioned the composition 4% copper, 4 to 6% silicon and less than 0.2% magnesium. This composition is an old one, is well known, and is widely used for applications other than patterns. There are several variations known as A108, 319, Allcast, Specification SC64B of ASTM, and numerous others. We have recommended it for patterns in certain cases but not in others and specific properties can be adjusted by slight variations in the composition. For example, the machineability is improved with higher magnesium contents although this also causes age hardening and embrittlement. It is usually good practice to keep the magnesium under 0.1% on this account.

Mr. Zukor makes two claims which in their fullest implication seem to be unsupported by factual data. It is true that all aluminum alloys have good corrosion resistance but those with copper in them are generally not as good as those without copper. His composition, therefore, would be on the poorer side as far as other aluminum alloys are concerned. It is also true that aluminum alloys can be melted and handled at low temperature compared to the copper base or ferrous alloys. To imply, however, that this particular composition can be handled at temperatures lower than the usual aluminum pattern alloys seems to us inconsistent with the facts.

Data to support the claims would tremendously strengthen this paper but inasmuch as many of them are contrary to experience, it is doubtful whether such supporting data is available.

¹ District Manager, Apex Smelting Co., Chicago.

AN INVESTIGATION OF MELTING AND CASTING PROCEDURES FOR HIGH PURITY NICKEL

By

Douglas W. Grobecker *

ABSTRACT

A suitable melting practice for the production of high purity nickel castings was found to be as follows: electrolytic nickel and returned scrap were melted in a monolithic sillimanite lined induction furnace, nickelous oxide was added during the melt-down to provide a strongly oxidizing condition during the initial stages of the melting cycle, carbon was added when the desired temperature was reached to cause a vigorous boil and begin deoxidation, small amounts of manganese and aluminum were added to further deoxidize the nickel, magnesium as a final addition was used to kill the heat, and the metal was poured immediately after the magnesium addition.

Returned scrap was used as a part of the charge for most of the melts. Nickel machine turnings, after suitable processing, were remelted as portions of charges.

Pouring temperatures were critical. Mold feeder head insulators were found to be necessary.

The mold materials tried were graphite, uncoated and coated with a fired magnesia wash; compacted carbon; core sand; and dry sand (air dried, skin dried, and oven dried).

The cast nickel showed some tendency towards intergranular weakness, but not sufficient to prevent the production of the desired castings. Photomicrographic studies indicated that some gas was retained in the cast metal.

Nickel castings were obtained with purity as high as 99.0 per cent nickel and free from defects when radiographically inspected.

As a quantity of nickel castings were required for experimental investigations, a program was initiated for the development of nickel casting procedures. The castings were to have as high a purity as was obtainable, and it was required that they be free of defects when radiographically inspected.

As the first phase of this program, a literature search was made to secure information which might act as a basis for the early work. Very little specific data on the casting of pure nickel were found; however, the following information was obtained:

1. Nickel melting procedures parallel those of steel.¹

2. Extreme care should be taken in the selection and preparation of the furnace lining, charge, and addition agents to avoid the pickup of harmful contaminents and to avoid gassy heats.^{1,2}

3. Harmful constituents of nickel include sulphur, selenium, tellurium, calcium, strontium, barium, sodium, potassium, lithium, hydrogen, and nitrogen.⁴

4. Allowance must be made in mold design to compensate for the high shrinkage characteristic and the narrow liquidus range of high purity nickel.¹

5. Additions of one or more of the elements carbon, silicon, manganese, titanium, boron, aluminum, phosphorus and magnesium are used to deoxidize and malleablize nickel and nickel alloy castings. 1,8,4,5

Induction furnaces, including a P-17 Ajax coil furnace powered by a 20 kw Ajax converter and a 100-lb Ajax furnace with a 50 kw Westinghouse motor generator set, were used to melt all nickel heats.

Melting stock was 99.9 per cent nickel electrolytic plate, 97 per cent nickel rolled plate, and returned scrap.

Considerable work was done to determine the most desirable furnace lining. Magnesia crucibles backed with magnesia sand were first tried. Due to low thermal shock characteristics, the magnesia crucibles could be used for only one or two heats before the crucibles cracked and required replacement.

A monolithic magnesia lining bonded with 1.2 per cent magnesium sulphate added as a water solution (I cc solution equals 0.2 gm of Mg₂SO₄) proved unsatisfactory. The monolithic magnesia lining had excellent heat shock characteristics and high resistance to erosion. However, this type lining had high thermal conductivity characteristics which necessitated long melt-down times, and produced castings which often cracked while cooling in the mold. The tendency for the castings to fracture was attributed to sulphur pickup from the lining binder.

Monolithic sillimanite proved to be the most satisfactory lining material, although subsequent cast metal analysis showed a slight silicon pickup (0.05 to 0.20 per cent). The sillimanite was rammed around graphite plugs to form the melting cavity. The linings were dried by heating graphite pieces to 2912 F (1600 C) for 1 hr in the lining cavity. Monolithic sillimanite linings had low thermal conductivity characteristics permitting short melt-down times, had good resistance to erosion, cracking, and breakdown at the required temperatures, and were capable of producing as high as 15 heats before requiring patching.

Graphite was used throughout most of the program as a mold material. Although early experiments indi-

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^{*} Los Alamos Scientific Laboratory, Los Alamos, New Mexico.

cated that graphite had a number of undesirable characteristics, it was the most readily available mold material.

The excessively high scrap loss of castings made in graphite molds prompted the investigation of sand as a molding material. After a molding procedure had been developed, sand molds proved quite satisfactory.

Refining and Deoxidizing Additions

Commercially cast pure nickel analyzes about 97 per cent Ni, 0.25 per cent C, 1.25 per cent Si, 0.5 per cent Mn, 0.5 per cent Fe, and 0.01 per cent S (max).⁶ As a higher purity was desired for the nickel castings, the first work was directed towards determining the minimum refining and deoxidizing additions required to give a sound casting.

Electrolytic nickel was used as melting stock for the first nickel heat. The charge was melted down, killed with a 0.5 per cent aluminum addition, and poured into a graphite mold which had been preheated to 1112 F (600 C). The casting analyzed 97.8 per cent Ni, 0.80 per cent Al, 0.012 per cent S, and 0.021 per cent C. The cause for the high aluminum and sulphur analysis was not determined, but later work indicated that the high aluminum content, as well as the sulphur content, and the use of a graphite core lacking collapsibility contributed to the cracking of the casting. The cracks which occurred around the cored section of the casting are shown in Fig. 1. The expected shrinkage in the feeder head of the casting was not present, indicating gassy metal of low density. The outer surface of the casting contained rough folds, as shown in Fig. 1, which seemed to be caused by the chilling effect of the graphite mold. The cored surface contained blows, $\frac{1}{16}$ in. to $\frac{1}{2}$ in. in diam, shown in Fig. 2, which were apparently caused by metal-mold reaction.

In later heats, carbon was added to the metal after melt-down to cause a boil and flush dissolved gases from the melt. The boil resulted from a reaction between the carbon and the oxygen in the melt forming



Fig. 1—Surface of first casting poured in the program. Severe cracking occurred in casting as it shrank around graphite core. Cracks due to low ductility of metal and lack of collapsibility in the graphite core.

Photo approx. 2x

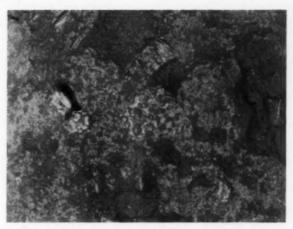


Fig. 2—Surface of contact between casting and graphite core of first casting produced in the nickel program. Blow defects due to metal-mold reaction and graphite from core, which had tightly adhered to the nickel.

Photo approx. 2x.

carbon monoxide. When deoxidized with small additions of silicon and manganese, the shrinkage characteristics of the metal were slightly improved, and the tendency for the casting to crack around cored sections was not as pronounced. However, the castings indicated that better results might be obtained by more thorough degassification.

In subsequent melts nickelous oxide was added during or immediately after melt-down in order to obtain more highly oxidizing conditions in the initial phases of the refining process. A strongly oxidizing condition was considered desirable for three reasons:

1. To oxidize impurities which in the oxidized state would rise to the melt surface.

2. To minimize hydrogen solution during the period while the melt was being raised to deoxidation and pouring temperature.

3. To purge dissolved gases from the nickel by causing a vigorous boil upon the addition of carbon to the melt.

Attempts to complete deoxidation with aluminum after the addition of carbon or after the addition of carbon, silicon, and manganese resulted in cracked castings. Small amounts of aluminum were later used to aid in the deoxidation process.

Magnesium Additions

Magnesium additions were tried to complete the deoxidation process after additions of carbon, silicon, manganese, and aluminum, and after additions of carbon, manganese, and aluminum. When 0.1 per cent magnesium was added, subsequent to a thorough boil and after the addition of preliminary deoxidizers, castings with sufficient ductility to prevent metal cracks were produced and deep piping in the feeder head, which indicated a dense casting, occurred.

Additions of titanium were tried to fix nitrogen¹ in the metal and aid in the deoxidation process; however, the use of titanium was abandoned as the addition served no apparent purpose in the production of suitable castings.

In later work the use of silicon was eliminated, as comparison between heats deoxidized with silicon and magnesium, and manganese and magnesium indicated that silicon promoted the tendency toward low ductility metal.

Systems for Making Additions

From observation, the solubility of nickelous oxide appeared to be a function of the metal temperature⁷ and the amount of impurities and carbon in the metal. During most of the melts, the nickel oxide went into solution readily when placed on the surface of the melt. During some of the heats, a large portion of the oxide mixed with the slag and failed to dissolve in the metal. When the oxide failed to go into solution, it was removed from the surface of the melt prior to the carbon addition. The oxide and slag mixture was removed by freezing to a cold nickel rod immersed in the slag.

Carbon in the form of dry, hardwood charcoal was added by placing it on the melt surface. Although the carbon burned while it was exposed to the air prior to solution, subsequent chemical analysis of the metal showed that the loss of carbon before solution was negligible. In some heats, particularly those where the carbon was added when the melt was at a high temperature, 3056 F (1680 C), the carbon reacted vigorously. Unless the carbon was added slowly and cautiously, the metal would rise and overflow the furnace.

In order to prevent oxidation of the pure metal deoxidizing additions by the atmosphere, it was necessary to plunge the deoxidizer additions beneath the melt surface. This was accomplished by binding the metal additions securely to a nickel rod with "A" nickel wire and quickly immersing the rod.

The addition of magnesium to the melt was normally accompanied by a violent reaction. To avoid eruption and splattering of the melt, it was required that the melt be as completely deoxidized as previous additions would provide, that the magnesium be securely bound to the plunge rod to prevent the metal addition from breaking away from the rod and rising to the surface, and that the magnesium be introduced into the melt as rapidly as possible.

Slag

Slag additions of pure lime and a low melting mixture of 30 per cent CaO, 7 per cent MgO and 63 per cent SiO₂ were tried in order to minimize gas solution by covering the surface of the metal. Experiments indicated that more gas was dissolved in the melt when a slag addition was used, possibly due to occluded water on the slag ingredients, than when no slag addition was made.

The most favorable slag was formed as a normal part of the melting procedure. Excess nickelous oxide, oxidized impurities in the scrap and eroded sillimanite lining combined to form a viscous slag which effectively coagulated loose slag particles. This slag was readily retained in the furnace during pouring. Rarely was sufficient slag formed to completely cover the melt surface. Because of the meniscus formed on the melt surface by electrically induced convections within the metal, the slag gravitated to the lining. About

half the melt surface was usually covered with slag. When the slag was voluminous or when it contained a high percentage of nickelous oxide, the slag was removed prior to the carbon addition.

Use of Scrap in Charge

Returned nickel scrap such as heads, risers, gates and scrapped castings were remelted successfully without appreciably lowering the purity of the metal. Scrap, comprising up to 50 per cent of the charge, was considered desirable due to the carbon content and

slag forming constituents available.

Turnings from machined castings presented a problem for re-use as machine cutting oils could cause gassy metal.² Turnings were successfully processed for remelting by cleaning for 8 hr in a trichloroethylene vapor degreaser, melting with 0.25 per cent nickelous oxide added to provide an oxidizing condition, and ingot pouring. The ingots were subsequently remelted as part of the charge for a production casting, and showed no tendency to produce gassy metal. Chemical analysis of a casting poured from a charge containing 15 per cent processed turnings showed 99.0 per cent nickel.

Pouring Temperatures

Nickel temperatures were measured with an optical pyrometer. Temperatures recorded in this report are optical readings taken on the surface of the metal in the furnace, corrected for an emissivity value of 0.60. The emissivity value was determined by correlation between the actual melting point of nickel at 2651 F (1455 C) and the optical reading at the melting point, 2516 F (1380 C).

When sand molds were used, pouring temperatures were held between 2858 F (1570 C) and 2948 F (1620 C). Higher temperatures promoted gas defects, misruns, sand washing, and penetration of the sand.

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Fig. 3—Surface of sand-molded casting from which thin machine cut had been taken. The defects do not extend deep into casting (1/8 in. max) and were not visible on as-cast surface. This defect occurred only when the metal was poured cold and is believed to be shrink or inadequate feeding within affected zone.

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at temperatures lower than 2858 F (1570 C) cavity defects occurred in each, as shown in Fig. 3. The irregular cavities, up to 1/8 in. in diameter, were not apparent on the casting surface, but occurred in a narrow zone just beneath the surface. These cavities did not extend into the casting, as would a pinhole defect. There was no indication of sand in the cavities, which would probably be present if washed sand were the cause. The molds were thoroughly dried prior to pouring. Although the cavity defects were generally limited to isolated areas immediately beneath the casting surface and did not occur in a center of mass, the cavities could possibly have been caused by shrinkage due to a lack of progressive solidification within the casting. The low pouring temperature, and the low heat conduction properties of the sand mold possibly combined to make a low thermal gradient between the surface and the center of the solidifying casting. The narrow liquidus range of the nickel could have prevented adequate feeding from the molten center of the casting to the affected zone.

When the nickel was cast in graphite molds, the pouring temperature was of necessity higher, 3020 F to 3074 F (1660 C to 1690 C), than for sand molds because of the chilling effect of the graphite.

Use of Feeder Head Insulators

The first castings poured in the program often contained shrinkage cavities in the main body of the mold beneath the feeder head. This shrinkage was caused by premature freezing of the head surface, limiting

adequate feeding of the casting. Simulating ferrous casting practices, lampblack was tried as a feeder head insulator. When lampblack was placed over the head immediately following pouring, the head was kept open, and shrinkage occurred from the top surface of the head downward forming an open pipe. The improved feeding of the head was credited to the insulating properties of the lampblack and its ability to go into solution and lower the freezing point of the metal. An eutectic is formed at 2.22 C in nickel which freezes at 2404 F (1318 C).9

Diatomaceous earth was tried as a feeder head insulator but was not as effective as lampblack.

Typical Heat Log

Typical of the heats which produced successful castings in sand molds was the following logged heat:

Charge—70 per cent electrolytic plate (99.95 per cent Ni) 30 per cent returned scrap (97 per cent Ni, 0.4 per cent C) Additions-Made in order. Percentages based on the weight of original charge.

0.5 per cent NiO (powder)

0.05 per cent Mn (electrolytic) 0.2 per cent C (dry, wood charcoal) 0.05 per cent Al (bar stock) 0.1 per cent Mg (bar stock)

| | Time | Temperature | | Remarks |
|---|------------|-------------|------|--|
| | | °F | °C | |
| 0 | | Room | Room | Furnace charged; current on. |
| 1 | hr, 7 min | 2651 | 1455 | Charge melting; remainder of charge added. |
| 1 | hr, 45 min | 2858 | 1470 | NiO added; mild boil. |
| 2 | hr, 5 min | 2876 | 1580 | C added; boil vigorous. |
| 2 | hr. 20 min | 2921 | 1605 | Mn and Al added. |
| 2 | hr, 22 min | 2912 | 1600 | Power cut; Mg added; metal poured. |

Graphite Molds

Although machined graphite molds were used during most of the development program, the successful application of graphite as a mold material for nickel castings were severely limited. The objections to graphite as a mold material were as follows:

1. A reaction between any oxygen remaining in the melt after deoxidation or picked up during pouring and the graphite mold produced gas at the metal-mold interface. The gas generally caused deep blow defects on the surface of the casting, as shown in Fig. 2. This reaction did not take place where the chilling effect of the graphite caused quick solidification, but was accentuated in localized "hot spots." The extent of the metal-mold reaction depended on the temperature to which the mold was preheated, the mold design, the pouring temperature, the completeness of deoxidation, and the amount of carbon in the metal. The last factor necessitated the melting of nickel with sufficient carbon to allow graphite precipitation upon cooling when graphite molds were used. Although the graphite in the nickel castings greatly improved machinability, it lowered density and purity.

2. Graphite molds were essentially impermeable to trapped gases; thus gases trapped at the metal-mold interface prevented smooth contact between the metal and the mold and caused surface irregularities.

3. The chilling effect of graphite promoted laps and cold shuts. It was possible to have cold shuts and "hot spot" reaction defects in different sections of the same casting.

Graphite Mold Washes and Coatings

Ceramic coatings on the graphite molds were tried to prevent direct contact between the nickel and the graphite mold. Experiments were conducted on a variety of coatings, but most washes had poor adherence after drying. The most promising coating was developed by the Ceramic Section under Adrian G.

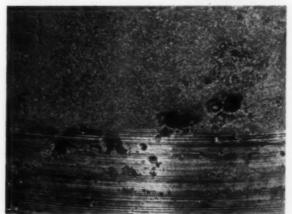


Fig. 4-Partially machined surface of nickel casting poured in magnesia-coated graphite mold. The pinhole defects extend 1/2 in. beneath surface and occurred in magnesia-coated graphite molds. Cause of defect believed to be moisture retained in mold coating or picked up by coating subsequent to firing. Photo approx. 2x.

Allison. Carefully graded magnesia sand bonded with magnesium sulphate solution was painted on the graphite surface. The coated mold was fired in a vacuum induction furnace at 2372 F (1300 C). The resulting coating was relatively adherent, and effectively prevented blows at the mold "hot spots." However, the coating often caused pinhole defects on the cope surfaces of castings, as shown in Fig. 4. These pinholes, often 1/2 in. in depth, were particularly prevalent under places where washed coat had collected on the surface of the casting. Although the molds were fired after the application of the magnesia coating, and were preheated 2 to 4 hr at 374 F (190 C) prior to pouring, it was assumed that the pinhole defects were caused by moisture either absorbed by the magnesia between firing and pouring, or retained in the coating during firing.

Compacted amorphous carbon molds and inserts were tried in order to take advantage of the permeability and low thermal conductivity characteristics of this form of carbon. The carbon molds proved unsatisfactory because the low thermal shock characteristics of carbon caused cracks in the molds and subsequent metal leaks. The carbon was subject to erosive attack by the flowing metal, and blow defects occurred

on the castings at the mold "hot spots."

Sand Molds

When it was apparent that suitable castings could not be consistently obtained with graphite molds, the investigation of sand as a molding material was undertaken. It was found that the following sand practices

generally yielded satisfactory castings:

1. The best castings were obtained by the use of washed and graded silica sand for facing material base. Ottawa Banding and Bond grades¹⁰ were used. The use of Banding grade silica sand generally gave a smoother surface finish on the casting, but Bond grade silica was less likely to give defects caused by low permeability.

2. Oven dried (overnight at 374 F, 190 C), torch skin dried (gas flame), and air dried (48 hr) molds

yielded satisfactory castings.

3. Organic binders were held to a minimum. First sand molds used were core molds containing core oil. All core molds and all subsequent molds utilizing core oil as a binding agent gave castings with surface blow defects. Small amounts of cereal binder used in conjunction with western bentonite as a sand binder proved satisfactory.

4. Water content of sands was held low. Any attempt to raise moisture content above 3½ per cent or to wet weak spots in the sand prior to drying, in order

to increase the mold strength, gave blows.

5. Ramming of the sand around the pattern was done with care. If rammed too tightly, the reduced permeability caused gas defects and uneven surfaces on the castings. These defects were more common when Banding grade sand rather than Bond grade sand was used, due to the lower permeability of the finer Banding sand. If the sand was insufficiently rammed, weak spots and subsequent washing and metal penetration of the sand occurred. Slight metal penetration of the mold and a comparatively rough

surfaced casting invariably resulted when Bond grade sand was used for a facing base.

6. Once the molding operation was started, it was completed as quickly as possible. If a comparatively small amount of moisture was lost during molding, the sand became difficult to work and the mold was almost impossible to patch.

7. Any "hot spots" in the mold were cored with an oven dried sand. The core was coated with a suitable wash to lower surface permeability, and adequately

rented

The facing sands found most applicable used Ottawa Banding silica, AFS Fineness 76, AFS Distribution 69, and 99.89 per cent SiO2 or Ottawa Bond silica, AFS Fineness 60, AFS Distribution 76, and 99.89 per cent SiO2 for a base.11 The base sand was bonded with a 4 per cent western bentonite, 0.5 per cent mogul corn cereal binder and 3.5 per cent water. 10,11 The mixtures were mulled in a Simpson Intensive Mixer. An AFS standard specimen of facing sand using Bond silica as a base tested 165 green permeability, 4.8 green compressive strength, and 85 dry compressive strength. A facing sand with a base of Banding grade silica tested 103 green permeability, 5.1 green compressive strength and dry strength greater than 90. These compositions worked well as both facing and core sand for casting.

The facing sand was backed in the flask with a mixture of Michigan City sand, using old facing sand, bentonite, mogul and sufficient water to temper. Backing sand tested green strength 3.5 and green perme-

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Castings were in sand molds with uncoated dry sand cores showed blow defects on the inner surface of the casting, as shown in Fig. 5. The defects were caused by excessive gassing of the cores, although the cores had previously been baked at 374 F (190 C) overnight. A number of wash coatings were investigated to prevent the gassing of the core. A commercial high temperature core wash proved ineffective in preventing the defects. Mixtures of silica flour, bentonite, and water were also unsatisfactory. Best results were ob-

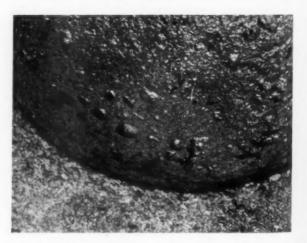


Fig. 5-Blow defects on cored surfaces of castings when uncoated, dry sand core was used.

Photo approx. 2x.

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tained from a wash containing 64 per cent zirconite flour, I per cent western bentonite and 35 per cent water by weight. The wash was painted on the green core which was subsequently dried at 374 F (190 C). After drying, a second coat of wash was applied to seal any cracks and the core was dried again.

In order to produce sound, defect-free castings, the design of sand molds must promote progressive solidification of the cast metal by allowing adequate heading and correctly positioning the gate and riser. Cores should be hollowed and adequately vented through the mold to prevent core blows. Castings made in properly prepared sand molds had surfaces with none of the defects exhibited by castings made in graphite molds.

Characteristics of Cast Nickel

Nickel demonstrated extremely high shrinkage characteristics during cooling in the liquid state, during solidification, and in the solid state. Pattern shrinkage allowances were high, $\frac{5}{16}$ in. per ft, and castings poured with thoroughly degassed metal showed deep piping effects in shrinkage heads, necessitating the use of tall risers.

If all of the metal for a casting was introduced through the sprue, and the heading was limited to reasonable proportion (1/4 of the mass of the rough casting), piping would generally extend beneath the feeder head. This deep piping effect could be minimized and acceptable castings obtained by pouring the metal through the sprue until the riser cavity had been filled, applying lampblack insulator to the riser, and making metal additions to the riser to compensate for feeding losses.

High purity cast nickel melted by the described methods exhibited a tendency towards intergranular weakness. This brittleness was not as severe as was encountered during the early stages of the program, but the lack of ductility was exhibited in a number of

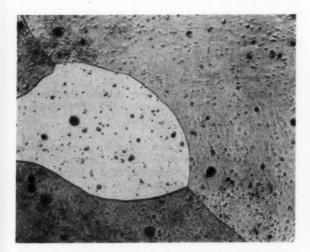


Fig. 6—Photomicrograph, 100x, of etched high purity nickel specimen. Specimen, 98.5% Ni, 0.14% T. C., no graphite. The small cavities due to gas and may be responsible for tendency towards intergranular weakness in the metal.

castings by fine hair line cracks which occurred when the nickel shrank around noncollapsible graphite cores.

The intergranular weakness was attributed to three possible causes:

1. Gas retained in the molten nickel or picked up during pouring and later evolved upon solidification may have been trapped at the grain boundaries. Photomicrographs, as shown in Fig. 6, indicated cavities that were probably caused by gas. Gas in the grain boundary region could cause intergranular weakness.

2. A second possibility to account for the intergranular weakness was the formation of a brittle or weak phase at the grain boundaries from one or a combination of the deoxidants and impurities. Sulphur content was not considered to be the cause of the brittleness, as the sulphur did not exceed 0.004 per cent in the nickel samples taken from castings poured in the latter part of the program.

3. A third possibility for the cause of intergranular weakness could have been the precipitation of graphite. This possibility was considered unlikely, as nickel had been cast over a wide range of carbon contents (0.08–0.6 per cent) with no apparent change in intergranular weakness tendencies. When samples containing sufficient carbon (over 0.3 per cent) to precipitate graphite detectable by chemical analysis were examined microscopically, the graphite generally occurred in a nodular form within the grains. Such a graphite distribution should not affect intergranular properties.

Microshrinkage was at one time thought to be a possible cause for the intergranular weakness. However, later work in which high purity nickel heats were melted and poured under vacuum into magnesia-coated graphite molds showed that pure nickel had good hot strength. The vacuum heats were melted without any additions for deoxidation or degassification. No cracks appeared in the castings.

The intergranular weakness of air melted nickel did not prevent the casting of the desired shapes, as all of the required castings could be cast in sand molds with collapsible cores.

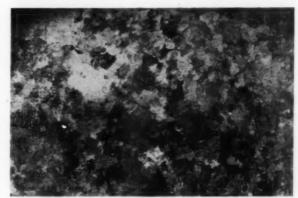


Fig. 7-Photomacrograph at 1.8x of machined and etched high purity nickel casting. Large grain size of the nickel shown. Horizontal striations were made by machining tool.

The grain size of high purity nickel melted by the desirable methods was large, as shown in Fig. 7. The grain size probably promoted the tendency towards intergranular weakness. No attempts to refine the grain of the castings was made, as it was felt that any addition which would promote grain nucleation and thus refine the grain structure would also lower the purity of the metal.

Chemical analysis of castings showed 98.5 to 99.0 per cent nickel, 0.05 to 0.3 per cent carbon, 0.15 to 0.25 per cent iron, 0.15 to 0.40 per cent silicon, 0.05 to 0.25 per cent manganese, 0.003 to 0.004 per cent sulphur, and cobalt (presence spectrographically determined) as the major impurities.

Visual surface inspection or radiographic techniques showed defects in most of the castings poured in magnesia coated or poured in uncoated graphite molds. Although all of the castings made in graphite molds were not radiographically inspected before the program was terminated, experience indicated that a yield of only 20 per cent could be expected. Sand molded castings which were radiographically inspected indicated that a yield of about 80 per cent satisfactory castings could be expected.

Conclusions

- 1. Induction electric furnaces, lined with monolithic sillimanite, were found suitable for melting high pur-
- 2. In order to obtain nickel suitable for pouring defect free castings, it was necessary to melt under highly oxidizing conditions, boil the melt thoroughly, and deoxidize with carbon, manganese, aluminum and magnesium.
- 3. The most desirable slag was formed during the melting period by coagulation of oxidized impurities, eroded sillimanite lining, and excess nickelous oxide.



Fig. 8-Photo shows defect-free, cored surface of nickel casting. Zirconite wash had been applied to the sand core. Photo 4x.

- 4. Scrapped castings, heads, gates, and processed machine turnings could be remelted as a portion of a subsequent charge without appreciably lowering the purity of the metal or causing gas defects.
- 5. Pouring temperatures were critical. The metal was poured at the lowest temperature at which the formation of cold shuts and cavity defects could be
- 6. The use of lampblack as an insulating cover for the feeder head was necessary to avoid excessively
- 7. The use of uncoated graphite molds was unsatisfactory. A reaction between the molden metal and the graphite produced severe gas defects.
- 8. Graphite molds coated with magnesia gave some satisfactory castings, but generally were subject to deep pinhole defects on the outer surface.
- 9. Castings made in properly prepared sand molds were generally free from defects.
- 10. Nickel melted and cast by the described methods showed a slight tendency to be brittle. The lack of ductility was not severe, and castings of the desired shapes could be cast in sand molds.
- 11. Castings containing 99.0 per cent nickel, 2 per cent higher purity than commercially pure nickel castings,6 could be obtained by the methods described.

Acknowledgment

The author expresses his appreciation for the technical assistance given him by members of the Los Alamos Scientific Laboratory.

Many helpful suggestions were made by Walter F. Arnold, Foundry Section Leader. Nickel melting and sand molding were done with the aid of Ralph A. Wicklin, Henry J. Abeyta, Joseph W. Taylor, Ramon I. Martinez, Orland E. Johnson, Martin L. Roach and Gilbert H. Ferran.

Magnesia coatings for graphite molds were developed and prepared by Adrian G. Allison, Ceramics Section Leader, and Marion R. Nadler.

Graphite molds were prepared in the Graphite Machine Shop under the supervision of Lester W.

Metallographic work was done by James F. Willging, Marvin J. Southard, William L. Gault and James D. Monk.

Chemical determinations were made in the Analytical Laboratory by R. L. Carpenter, E. B. Crone and C. H. Ward under the direction of Charles F. Metz and W. H. Ashley.

Radiographic inspections of castings were done under the supervision of Gerald H. Tenney.

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Assistance in the preparation and editing of the paper was received from James M. Taub, Denton T. Doll, Walter F. Arnold and Eric R. Jette.

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12. R. W. Schroeder, University of Illinois, Personal Correspondence, April 22, 1949.

DISCUSSION

Chairman: J. J. Curran, Walworth Co., Greensburg, Pa. Co-Chairman: A. K. Higgins, Allis-Chalmers Mfg. Co., Milwaukee.

R. C. Tibbetts: 1 What was your melting time?

Mr. Grobecker: Our melting time was approximately 2½ hr for a 100-lb melt. This period includes melting and processing of the charge.

MR. TIBBETTS: Isn't that rather excessive?

MR. GROBECKER: Lengthy melt down times were necessary as the power input to the coil was timed to 25 kw or less throughout most of the melting cycle. Our power source was designed for and mainly used with graphite heaters. For nickel melts the charge was the only susceptor which imposed difficulties in maintaining a balance between voltage and amperage limitations.

Mr. Tibbetts: Did you not find the same result in the crucibles?

Mr. Grobecker: The power input was limited by characteristics of the power source, the coil, and the charge and was not affected by the ceramic crucible material.

Mr. TIBBETTS: I understand you had a small tilting furnace.

MR. GROBECKER: That is right.

Mr. TIBBETTS: We were using an induction furnace and were contemplating melting pure nickel. We were told that it would be difficult to do in this furnace; that we may have to change our coils.

MR. GROBECKER: It should not be necessary for you to change coils providing your present coil was found suitable for metal melting. The difficulties you will encounter causing longer melt down times will probably be caused by the higher melting temperature of nickel.

Mr. Tibbetts: Did you build your coil especially for melting nickel?

Mr. Grobecker: No, it is a commercial coil produced by Ajax.

MEMBER: Did you melt the nickel under any protective flux or cover of some kind?

MR. GROBECKER: In a number of cases we tried lime coverage and mixtures of low melting oxides but we found them unsatisfactory. There was some indication that the added slag constituents caused rather than prevented gassy metal. Our best slag cover was formed naturally during the melting cycle. Excess nickel oxide, eroded sillimanite lining and oxidized impurities from the scrap formed a viscous slag that would partially cover the melt.

MEMBER: You mentioned that since there is a very high shrinkage in nickel on solidification, the risers would have to be considered. Would you say there is any standard ratio of area of risers to the casting itself?

MR. GROBECKER: We found that shrinkage tendencies varied greatly and were primarily dependent on the thoroughness of degasification. There would be other factors that would also vary the ratio between the head and casting weight, such as the type of casting and whether you could allow internal shrinkage defects.

H. C. Chaden: ^a Have you noticed the effect of hard ramming of the sand?

Mr. Grobecker: We did have some trouble with tight ram-

ming, but we found the defects were more dependent on the sand mixtures than on how they were prepared. High permeability, washed, and graded silica sand was used as a base. The use of core oils were avoided and organic binders were held at a minimum to avoid surface blows.

G. P. HALLIWELL: ^a On the first page of this paper the author mentions harmful impurities. I believe he gets that information from a reference. Among the harmful impurities he mentions calcium. It so happens there are a number of deoxidizers on the market for nickel and nickel alloys which contain calcium. Somebody is wrong. Can you clarify this point?

Are the low ductilities you have been getting not in part due to some carbide being precipitated in the grain boundaries? We know that carbon is not good for wrought nickel and nickel allows.

MR. GROBECKER: As mentioned in the paper, the fact that calcium is a harmful constituent in nickel was reported by C. G. Bieber in U. S. Patent 2,150,094, March 7, 1939. We did no work during this program which would confirm or invalidate this statement.

Concerning the effect of precipitated carbon on the ductility of nickel, our investigation indicated that it was slight. Microscopic examination and chemical analysis of the cast nickel showed graphite when the carbon content exceeded 0.3 per cent. Melts were made with the carbon content varying from 0.05 to 0.60 per cent without noticeable difference in ductility.

CHAIRMAN CURRAN: In looking over your paper I see what appears to be an answer to Mr. Chaden's question.

MR. GROBECKER: That statement is ambiguous. I intended to convey that 80 per cent of the castings poured would be expected to pass inspection.

Member: In what proportion do you add your constituents to the melt?

Mr. Grobecker: Nickel oxide, 0.5 per cent; carbon, 0.2 per cent; manganese, 0.05 per cent; magnesium, 0.1 per cent.

MEMBER: Is that your only addition for nickel?

Mr. Grobecker: Yes. We were attempting to obtain nickel with as high purity as possible.

MEMBER: That is what I have been attempting to do too. You added nothing else and had a good dead metal?

Mr. Grobecker: Yes, we had no trouble with wild metal when the metal was melted and treated as described.

MEMBER: I have melted down several hundred pounds of nickel and I had difficulty at first but the problem seems to have straightened itself out. I used magnesium and I wondered whether that was standard practice.

MR. GROBECKER: I believe it is standard practice. We found out it was absolutely necessary as far as we are concerned. We could get no place until we deoxidized with magnesium.

MEMBER: When we introduced it we had a vigorous reaction,

and the only thing you can do is plunge it in.

MR. HALLIWELL: You say you want nickel of as high a purity as possible. Is that consistent with your procedure of adding aluminum, carbon, magnesium, etc., for purposes of deoxidation. You certainly do not have high purity nickel. What you are trying to do apparently is to get nickel with low sulphur or gas content, but I think that it is misleading to speak of high purity nickel, which contains the amounts of residual metals that you indicate.

MR. GROBECKER: We found we could not get metal properly degassed and deoxidized without adding these constituents. We kept the additions at a minimum to maintain purity as high as possible. Actually we expected our sulphur content to be low as no constituent containing appreciable sulphur was added to the charge. The magnesium addition would be expected to multiply the effect of the sulphur if appreciable amounts were present. However, the main function of the magnesium is, I believe, to complete the deoxidation process.

MEMBER: Would a 0.50 per cent carbon addition give you a good, solid casting in your estimation?

Mr. Grobecker: We had good castings with that composition but the microstructure was poor. If you want to keep the carbon content below precipitation level hold the carbon content below 0.30 per cent.

¹ Vice-President, Wollaston Brass & Aluminum Foundry, No. Quincy, Mass.

² Superintendent, South Bend Smelting & Refining Co., South Bend, Ind.
³ Director of Research, H. Kramer & Co., Chicago.

MEMBER: What percentage of the carbon addition remains in the metal after solidification?

MR. GROBECKER: Metal melted and treated as described in this paper generally contained around 0.15 per cent carbon. The scrap portions of the charges generally contained high carbon from the lamp black insulating covers placed on the heads. However, most of the carbon in the original charge and added subsequent to the melt down was lost during the boil.

MEMBER: If you started out with virgin material completely would you not have that carbon to start with? Would you then use the same amount of nickel oxide or would you use less?

Mr. Grobecker: We standardized on an addition of 0.5 per cent nickel oxide for all melts. We were desirous mainly of establishing an oxidizing condition during the early stages of the melt and 0.5 per cent nickel oxide proved sufficient for this pur-

MEMBER: Would charcoal be the best thing to add with graphite?

MR. GROBECKER: I believe dry, hardwood charcoal would be best. That is probably your most readily available material. The only precaution would be that it must be dry and low in

MEMBER: The charcoal goes into such violent boil immediately. Would something else be better?

MR. GROBECKER: We had this trouble. Often the metal almost boiled out of the furnace when the carbon was added too rapidly. We controlled the boil by adding the carbon slowly. It was not necessary to add the carbon all at once.

M. G. Dietl: 4 I had occasion to make pure nickel castings with 97 plus per cent of nickel. In order to get good castings, we added carbon in the form of Barrett cupola coke, ground up dry, and introduced into the indirect arc furnace. Castings and subsequent test bars were made. The castings were tight, and had very good shrinkage. The test bar had a tremendous amount of ductility; it could be bent more than 180 deg. The carbon content was over 0.50 per cent. I do believe that low ductility is caused not so much by graphite precipitation in the grain boundaries as it is by gased metal.

CHAIRMAN CURRAN: You mentioned Barrett coke. That would

mean a petroleum coke containing very low sulphur.

Mr. Dietl: Very low sulphur is mandatory.

Ben Spindler: ⁵ What were the molds made of in which Mr. Dietl made his nickel castings?

Mr. DIETL: The molds were made of sand of very high permeability on the order of 45 to 55.

Mr. SPINDLER: Was that a natural bonded sand?

MR. DIETL: It was a synthetic bonded sand with western bentonite. The strength was up around 22. The sand also had 0.5 per cent cereal binder to get away from the sand defects that might develop. It was rammed very hard. Apparently, the hard ramming gives a better thermal conductivity. Moisture is kept relatively low, about 3 to 3.5 per cent.

MEMBER: Did you get a vigorous oxidation in the metal? Mr. DIETL: Vigorous oxidation in the ladle is preferred.

MEMBER: How much silicon did you use?

Mr. DIETL: We added 0.50 to 0.70 per cent silicon.

MEMBER: Was the silicon used to quiet down the boil with that high carbon content?

MR. DIETL: Apparently the residual silicon in the metal does not help much in quieting down the metal. I prefer to kill the heat with magnesium additions, and use the silicon to build the strength of the alloy.

MEMBER: We have at times seen it start to build after we have started pouring molds.

MR. DIETL: Does it boil in the ladle? What kind of transfer ladle did you use?

MEMBER: It is lined with silica sand bonded with clay, and thoroughly dried.

MR. DIETL: In our experience it became necessary to use claygraphite ladles and the very same reaction was noted. As pouring of the molds continued the burning into the sand became more and more severe. Discontinuing the use of the claygraphite liners cleared up the "wild" metal condition.

CHAIRMAN CURRAN: I have been interested to notice that both the author and Mr. Dietl believe the chief function of magnesium is to deoxidize. Perhaps that does account for the violence of the reaction you get in most of the nickel alloys, the nickelcopper alloys at least, because we can make additions of magnesium with little or no reaction. It must mean in the case of the nickel at the time you add your magnesium it is in a fairly highly oxidized state and therefore the magnesium does deoxidize.

⁴ Nonferrous Melting Foreman, Crane Co, Chicago.

Foundry Manager, Alloy Steel Products Co., Bloomfield, N. J.

MAGNESIUM FOUNDRY PRACTICE IN CANADA

By

M. W. Martinson* and J. W. Meier**

AN EVALUATION of the current and potential future status of the magnesium casting industry in Canada requires an examination of the industry from its inception, and of the various economic factors which will have an important bearing on future growth.

Although Canada pioneered in production of magnesium and magnesium alloys on this continent, its growth in the industry has lagged behind that of the United States. Magnesium was first produced in Canada at Shawinigan Falls, Quebec, by Shawinigan Electro Metals Company, in 1915. The highest production rate obtained by this plant was approximately 600 lb per day (10 tons per month). Production during the war years of 1915-1918 was used entirely for powder (pyrotechnics) and for aluminum alloying purposes. At the end of the war an effort was made to develop and maintain the industry, the chief development being magnesium pistons. On July 1, 1919, magnesium production in Canada ceased. By 1938 the annual consumption of magnesium in Canada was about 5 tons.¹

World War II brought an urgent demand for domestic magnesium production and the Canadian Government, to satisfy this need, formed a Crown company, Dominion Magnesium Ltd., and built a plant at Haley, Ontario, which began operations late in 1942. This company, which was taken over in 1945 by private capital, uses the Pidgeon Ferrosilicon Process.² Maximum production rate of this plant was in the order of 15 tons daily (5000 tons per year). In addition, Aluminum Company of Canada Ltd. at Arvida, Quebec, has produced magnesium in an electrolytic plant rated at 1000 tons per year maximum productive capacity.

Although the 1949 Penton's Foundry List shows 13 foundries capable of producing magnesium castings in Canada (as compared to 138 in the United States),

to the writers' knowledge there are only six foundries in Canada that are in a position to supply magnesium castings.

Production Statistics

A review of magnesium casting production statistics for Canada and the United States indicates quite clearly that the size of the industries in the two countries is of entirely different orders of magnitude. This quite naturally can be traced back to the relative domestic consumption of finished goods. Since the present domestic consumption is considerably smaller than the ingot production capacity, Canadian industry depends upon export trade. Under present tariff conditions it is difficult for Canadian magnesium to enter the United States. At the Geneva Conference in 1948 the United States Government agreed to reduce the tariff on magnesium entering the United States from 40 cents per pound plus 20 per cent ad valorem to 20 cents per pound plus 10 per cent ad valorem, so that the duty is still more than 100 per cent of the value of magnesium entering the United States.

Table 1 shows the estimated Canadian and American casting productions on a relative per capita basis. This shows that although in magnesium production Canada has been behind the United States in proportion to population, since 1945 its consumption of magnesium castings has increased quite rapidly, considering that there was negligible domestic consumption prior to this time.

TABLE 1-SAND CASTING PRODUCTION, 1946-1948

| | 1946 | 1947 | 1948 |
|-------------------------------|------------|-------------|---------|
| Canadian Production, 1b | 157,000 | 260,000 | 450,000 |
| Canadian Production per | | | |
| 1,000,000 population, lb | 12,800 | 20,600 | 35,000 |
| Average U. S. Production,* lb | | 7,000,000 | |
| Average U. S. Production per | | | |
| 1,000,000 population, lb | | 48,000 | |
| * Approximate yearly av | erage fron | 1946 to 194 | 8. |

Foundry Techniques

So much for the background: our subject is Canadian foundry practice in the field of magnesium sand casting. As shown in Table 1, the production of Cana-

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Research Metallurgist, employed by Dominion Magnesium Limited at the Physical Metallurgy Research Laboratories, Bureau of Mines, Ottawa.

^{**} Head, Non-Ferrous Metals Section, Physical Metallurgy Research Laboratories, Bureau of Mines, Ottawa.

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dian magnesium foundries was nearly 450,000 lb in 1948 and the greatest percentage could be accounted for by two foundries. This is small in comparison with the production capacities of the larger magnesium foundries in the United States. These smaller production rates were major factors in establishing Canadian foundry techniques, which had to be based on smaller size foundry equipment and, in most cases, on manual rather than mechanical handling methods.

In general, Canadian magnesium foundry practice^{3,4} was developed as a combination of techniques that were established earlier in Great Britain and in the United States and adapted to local conditions. Geographic and economic factors introduced special problems into the newly created industry which could not be considered during the war years. Fortunately, the still rather small size of this industry at the end of the war has since worked to advantage in allowing the Canadian magnesium industry to become firmly rooted and to avoid the additional difficulties due to overexpansion during the war.

Reconversion to peace-time production was aided considerably by cooperative research and development work on magnesium alloys carried out at the Physical Metallurgy Research Laboratories of the Canadian Bureau of Mines, Ottawa.5 This broad research program, started in 1945, included also extensive work on the techniques of melting, refining, and casting of standard magnesium casting alloys. The aims of this program were, first, to evaluate achievements of British and United States foundry research and practice as adapted to local conditions, and later, to establish standard procedures suitable for Canadian foundries.6,7,8

This article is limited to foundry techniques used in the production of sand castings from standard magnesium casting alloys, based on the magnesium-aluminum-zinc alloy system, and does not include special methods used in handling the newer alloys containing cerium, zirconium, etc. The development of the latter alloys is being reported separately.9

Alloys-One of the most important advances made in magnesium alloys in recent years has been the improved corrosion resistance of casting alloys realized by limiting such impurities as iron, nickel, copper,

phosphorus, etc.

Canadian foundries have at their disposal high purity vacuum-distilled magnesium, as produced by the ferrosilicon process, as shown in Table 2. It is a wellestablished fact that iron and nickel contents above 0.002 per cent accelerate considerably the corrosion rate.10 There is no trouble in maintaining this high purity in the commercial alloy ingots or castings.6

TABLE 2-TYPICAL ANALYSIS OF DOMAL HIGH PURITY MAGNESIUM

| | Per Cent | | Per Cent |
|-----------|----------|---------------------|----------|
| Aluminum | 0.004 | Zinc | 0.001 |
| Nickel | 0.0005 | Calcium | 0.004 |
| Copper | < 0.001 | Lead | 0.001 |
| Iron | 0.001 | Sodium | < 0.001 |
| Manganese | 0.002 | Phosphorus | 0.001 |
| Silicon | 0.004 | Total | |
| | | Impurities, approx. | 0.020 |

TABLE 3-COMPOSITIONS OF DOMAL MAGNESIUM CASTING ALLOYS

| Domal* (ASTM) | Aluminum | Zinc | Manganese, min | | Total Fe+ Ni+Cu, In max | Total Other mpurities max |
|------------------|----------|-----------|-------------------|-------|-------------------------------|------------------------------------|
| AZ80X | 7.8-9.2 | 0.2-0.6 | 0.18 | 0.003 | 0.005 | 0.045 |
| AZ91X | 3.5-9.5 | 0.2 - 1.0 | 0.20 | 0.003 | 0.005 | 0.045 |
| AZ63X | 5.5-6.5 | 2.7-3.3 | 0.18 | 0.003 | 0.005 | 0.045 |
| AZ92X | 8.5-9.5 | 1.7-2.3 | 0.13 | 0.003 | 0.005 | 0.045 |

(* Alloy designations used in this paper are based on the ASTM code. "X" after the alloy number indicates high purity as used formerly in ASTM specifications and discontinued in 1948 because high purity casting alloys in the U.S.A. were commercially

Perhaps the outstanding divergence in Canadian practice lies in the selection of foundry alloys. The alloys used in Canadian foundries are similar to European practice in the use of magnesium-aluminum alloys with low zinc (AZ80X and AZ91X). In contrast, the higher zinc containing magnesium alloys AZ63 and AZ92 are the most popular casting alloys in the United States. It is considered that AZ80X is comparable to AZ63 in the as-cast (AC), heat-treated (HT), and heattreated-and-aged (HTA) condition and is the most versatile foundry alloy. It combines high strength, ductility, pressure tightness, freedom from microporosity, resistance to shock, and has good weldability. For alloys requiring high yield strength, AZ91X is used, rather than AZ92, in the heat-treated-and-aged condition, because it is less susceptible to microporosity.

Another important consideration in favor of the low zinc type of alloys is their amenability to heat treatment. The minimum eutectic melting temperature of AZ80X and AZ91X is about 425 C (800 F), which is above the normal heat treating temperature, so that these alloys can be heated to solution temperature without fear of damage, and necessity of a protec-

tive atmosphere is not as critical.

Particularly on aircraft material, there are sometimes difficulties in obtaining approval for change from specified AZ63 or AZ92 to AZ80X and AZ91X alloys when the original specifications for casting originate in the United States. However, it should only be a matter of time before the present commercial highzinc alloys AZ63 and AZ92 will be replaced by the easier casting and more reliable low zinc alloys in that country.11 Most new specifications being drafted in Canada concern use of the low-zinc type alloys.

Normally, cast-to-shape test bars, Dow type,12 are used in preference to British D.T.D. type machined test bars. It has been found that Dow bars give more consistent results and are more representative of melt quality, which, of course, should be the chief function of a cast test bar.

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Table 4 shows typical mechanical properties of Domal magnesium casting alloys.

Handling Equipment-All magnesium foundries in Canada are jobbing foundries, being equipped to handle efficiently match plate molding and medium floor molding jobs. At the present time, there are only one or two foundries equipped to handle medium

TABLE 4-TYPICAL MECHANICAL PROPERTIES OF DOMAL MAGNESIUM CASTING ALLOYS

| | | As-Cast | | | Heat Treated | | | Heat Treated and Aged | | |
|-------|------------------|---------------------|----------------------|------------------|---------------------|----------------------|------------------|-----------------------|----------------------|--|
| Alloy | U.T.S. (Kpsi) | 0.2% P.S. (Kpsi) | Elong. % on 2-in. | U.T.S. (Kpsi) | 0.2% P.S. (Kpsi) | Elong. % on 2-in. | U.T.S. (Kpsi) | 0.2% P.S. (Kpsi) | Elong. 9 on 2 in. | |
| AZ80X | 26.5 | 14.0 | 5.0 | 38.0 | 14.0 | 12.0 | 38.0 | 18.0 | 5.0 | |
| AZ91X | 24.0 | 14.0 | 3.0 | 38.0 | 15.0 | 10.0 | 37.0 | 21.0 | 3.0 | |
| AZ63X | 28.0 | 13.0 | 6.0 | 40.0 | 13.0 | 12.0 | 39.0 | 18.0 | 6.0 | |
| AZ92X | 24.0 | 15.0 | 2.0 | 40.0 | 15.0 | 10.0 | 39.0 | 22.0 | 3.0 | |

to large sized machine molding jobs, as to date casting requirements in Canada have not been such as to warrant the necessary equipment.

Three foundries are equipped to handle aircraft castings, either as green sand or dry sand assemblies. These foundries are either completely or partly mechanized, having mechanical sand handling equipment and conveyor systems for the molds.

Conditioned sand is supplied to the molding machine from overhead hoppers fed by a conveyor belt carrying sand from the sand preparation plant. Each molding machine is provided with ample roller conveyors for removing the molds from the machine. These side arteries feed the main pouring conveyor, the molds being so arranged as to compensate for a metal temperature drop from the beginning to the end of the pouring operation (Fig. 1).



Fig. 1-Molding bay in Canadian magnesium foundry.

Patterns—Pattern equipment designed for aluminum alloys is in many cases used interchangeably for magnesium although in an effort to avoid misruns the minimum section thickness considered for magnesium sand castings is about 5/32 in. Thinner sections should be confined to small areas. Every effort is made to maintain sections as uniformly as possible. Generous fillets or tapers are used for changes in sections. The shrinkage allowance generally used for magnesium alloys is 5/32-in. per ft under conditions of free shrinkage, and 1/36-in. per ft for restrained shrinkage.

The life of wood pattern equipment depends upon the construction, the manner of handling, and the method of storing. In most cases, storage facilities having controlled temperature and humidity conditions are provided. Molding Sand—Until quite recently all sands used by magnesium foundries were imported from the United States. By the time the sand arrived at its destination the cost varied from \$10 to \$17 per ton. This high cost of a basic material made itself felt, particularly to the small foundryman and to the dry core sand assembly foundry. The increased demand for synthetic foundry sand has finally been responsible for finding a Canadian source of silica sand suitable for magnesium molding. This new source of supply, besides providing a Canadian supplier, saves the foundry approximately \$4 to \$8 per ton of sand on freight rates and dollar exchange.

All magnesium foundries in Canada are using synthetic sand mixtures, based on washed and dried, subangular silica sand of A.F.S. fineness No. 65 to No. 70. Contrary to American practice, only sulphur and boric acid type inhibitors are being used. Generally, the bond is 50:50 western and southern bentonite, and diethylene glycol is used to prevent the molds from drying out too rapidly. The chief concern is casting finish, consistent with sand handling.

Table 5 gives typical characteristics of mold sand used in most Canadian foundries:

TABLE 5-TYPICAL CHARACTERISTICS OF MOLDING SAND USED

| Composition: | | |
|--------------------------------------|-------|-----------|
| Silica Sand (A.F.S. Fineness No. 70) | | 100 lb |
| Bentonite | | 4 lb |
| Sulphur | | 4-5 lb |
| Diethylene Glycol | | 0.5-1 lb |
| Boric Acid | | 0.5-1 lb |
| Properties (average): | | |
| Green Permeability | A.F.S | 6. 80-100 |
| Green Compressive Strength, psi | | 7- 10 |
| Moisture, % | | 2.5- 5 |

In the mechanized foundry the sand is passed from shakeout over a vibrating screen to remove extraneous materials and chills, and thence to storage bins. It is reconditioned by remulling dry for 2 min, followed by a few minutes of wet mulling. All the foundries in Canada have sufficient mulling equipment for insuring uniform sand mixtures.

Moisture content is important and is checked several times a shift at the muller by the calcium carbide method.

In the mechanized foundry the bond in the molding sand is adjusted to compensate for the movements of mold conveyors which might result in sags or drops.

Constant additions of new sand and inhibitors are made to maintain sand properties.

In all the foundries all operations and additions are standardized so that uniform sand characteristics can be maintained. Routine analyses are made to check the inhibitor content (sulphur, boric acid, diethylene glycol) to ensure uniform casting conditions.

One foundry uses new facing sand on all molds, to ensure uniformity of quality and surface finish, as well as an additional precaution against porosity and blow holes. By using facing sand the backing sand qualities are rendered less important and wider varieties of properties can be tolerated. Regular use of new facing sand maintains the desired inhibitor content in the backing sand within required limits due to diluting. This method also relieves mulling facilities, as the backing sand can be prepared by hand.

Core Sand—When considering core sands different problems are encountered than with molding sands because cores must be substantial enough to be handled and yet must have good permeability and collapsibility. All the foundries use a basic mixture incorporating a base silica sand of A.F.S. Fineness No. 65 to No. 80, urea formaldehyde in conjunction with a dextrine-type cereal binder, and the customary inhibitors, sulphur and boric acid. The use of silica flour, or fly ash, is optional. Most bench core jobs are maintained at 31/2 to 6 per cent moisture content. Very few cores are blown, but in such cases the moisture content is of the order of 2 per cent. However, as is customary in all foundries, there are different mixtures to meet different needs.

Generally, careful core practice ensures minimum casting rejects attributable to cores. Urea formaldehyde dry type binders baked at 160 C (320 F) can be stored indefinitely without fear of moisture pick-up.

Table 6 lists two typical mixtures used in Canadian foundries. The former is a standard mixture used for small cores, and the latter is for larger cores subjected to considerable handling and requiring dimensional stability.

TABLE 6-TYPICAL CORE MIXES AND PROPERTIES

| Composition | Stand | ard | Stron | ıg |
|--------------------------------------|--------|-----|-------|----|
| Silica Sand (A.F.S. Fineness No. 70) | .100.0 | lb | 100.0 | lb |
| Sulphur | . 1.3 | lb | 1.3 | lb |
| Boric Acid | | lb | 1.3 | Ib |
| Cereal Binder | . 0.5 | lb | 1.1 | lb |
| Urea Formaldehyde (Dry) | 0.5 | lb | 1.3 | lb |
| Fuel Oil | | lb | 0.6 | lb |
| Properties (average) | | | | |
| Moisture, % | 4.0 | | 4.0 | |
| Green Compressive Strength, psi | | | 1.3 | |
| Baked Tensile Strength, psi | | | 250.0 | |
| Baked Hardness | | | 95.0 | |
| Baked Permeability | 150.0 | | 160.0 | |

It is unfortunate that most of the small foundries interested in magnesium are not in a position to carry too much technical supervision and therefore their foundry control must be maintained by outside laboratory facilities.

Gating and Risering—Magnesium foundrymen soon recognized the desirability of sound gating and risering practice for producing castings economically. Since liquid magnesium alloys react with air in the mold to form oxides, it is essential to reduce the metal turbulence in the gating system and prevent the washing of oxide skin into the casting. The selection of the gating and risering system is generally left to the dis-

cretion of the molding superintendent who, in conjunction with the patternmaker, finalizes the set-up by permanently attaching gates and risers to the pattern. The final arrangement is usually arrived at by the judicious application of fundamental principles and practical experience.

Within the past few years there have been several excellent papers on gating techniques, and much has been learned by reviewing them. Canadian gating and risering practice simulates United States rather than European practice. Horn gates and multiple slot sprues with double and even triple runner systems are seldom if ever used. Bottom gating is preferred, and a number of practical rules for gating have been evolved, namely:

1. The castings should be filled through multiple small gates rather than a few large ones.

The cross-sectional area of the gates should be approximately equal to the cross-sectional area of the runners.

3. The runner should be greater in depth than width and should extend beyond the last gate. This, of course, is not possible in the case of a ring runner extending completely around the casting. Here the gates are uniformly distributed around the circumference and usually slope backwards into the casting.

4. Always choke the metal in the sprue.

The use of the low-zinc alloys with less tendency to hot shortness has been responsible for a certain amount of relaxation from these fundamental rules, to allow higher metal yields. Perforated skim gates at the base of the sprue, preferably placed vertically, usually trap and eliminate dross caused by faulty pouring practice. Steel wool is seldom used, and always in conjunction with vertical skim gates.

On larger castings and particularly aircraft castings (see Fig. 2), requiring maximum soundness, the side gating or conventional slot gating is used similar to techniques described by Beck¹³ or by Elliott and Mezoff.¹⁴ Essentially this system makes use of a slot gate attaching the casting to a reservoir (well). An annu-



Fig. 2-Magnesium casting for aircraft industry.

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lar screen is placed concentric with the well. Whether the slot sprue is fed by the bottom or by the top gating, it allows for a reduction in chilling and for uniformity of quality from one casting to the next, and invariably has resulted in increased casting yields.

A well designed gating system is generally supplemented by the judicious use of chills of aluminum or cast iron, and properly placed risers to feed the heavier sections. The chills are sandblasted prior to being placed in the mold. Risers are generally connected quite closely to the sections that require feeding and are of sufficient size to remain molten until the casting has solidified. If needed, wrapped risers or exothermic compounds are used to delay solidification of the riser and allow for more adequate feeding.

The metal is poured directly into the sprue on small bench molds, but for larger molds pouring basins are resorted to. It is essential to fill the pouring basin at the beginning of the casting operation in order to prevent oxides, formed during pouring, from entering

the sprue.

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Melting-In the largest foundry, the charge is melted in a tilting furnace of 600 to 800-lb capacity, using an approximate charge of 30:70 of ingot and sand blasted scrap. The charge is melted under a cover of melting flux (Domal fluid flux, see Table 7) which remains liquid throughout the operation and helps to cleanse the melt of dross and dirt. The maximum temperature reached in the bulk melter is 750 C (1380 F). After the melting operation is completed, the metal is poured off into fabricated steel crucibles ranging in capacities from 90 to 180-lb. These crucibles are transferred to individual furnaces for the refining and holding periods. It is felt that the handling of magnesium in the bulk melter, prior to transfer, helps maintain the silicon content constant, as the silica sand from sand blasting or adhering molding sand settle out. This avoids silicon pick-up in the metal, if for any reason the melt temperature is in excess of 815 C (1500 F), the approximate temperature at which magnesium begins to reduce SiO2 to Si.

In smaller foundries the melting and refining operations are carried out directly in stationary, oil-fired

crucible furnaces of 90 to 100-lb capacity.

Refining—There are several common methods for fluxing, degassing, and grain refining of molten magnesium alloys and, consequently, there has been a difference of opinion in Canadian foundries as to their use in this regard. Considerable development work on various refining methods, proposed in the last ten years by various workers in the United States and in Europe, was carried out at the Canadian Bureau of Mines, 6,7 and checked in commercial foundries.

Initially solid flux refining and superheating were used in all Canadian foundries. Later, however, all foundries, with one exception, discontinued superheating in favor of grain refinement by carbon inoculation and introduced degassing of the molten metal using chlorine or chlorine compounds. The latest development in refining practice, since approximately two years ago, is to combine grain refinement and degassing by the use of hexachlorethane additions. Although this method is not used extensively in the United States, most Canadian foundries are using it

with generally satisfactory results. The efficiency of hexachlorethane refining depends on:

1. Size of the melt,

2. Type and size of castings handled,

3. Condition of scrap added to charge, and

4. Alloy composition.

The following short review of the various refining methods used commercially in Canada may be of interest:

Solid Fluxes—Solid fluxes used at present mainly as cover fluxes, are listed in Table 7.

TABLE 7-FLUX COMPOSITIONS

| | MgCl _s | KCI | CaO.MgO | CaF ₂ |
|------------------------|-------------------|-----|---------|------------------|
| Domal Fluid Flux, % | 60 | 40 | _ | _ |
| Domal Crucible Flux, % | 50 | 20 | 15 | 15 |

Superheating—Crucibles, after being transferred to refining furnaces, are flux refined, that is, refining flux is manually stirred into the metal until the surface of the melt is silvery in appearance. This flux refining is done at temperatures of 730 to 750 C (1350 to 1380 F) and takes from 2 to 5 min. Next, the crucible is sludged, and a layer of flux ½-in. by ¾-in. thick is added which is allowed to remain on the surface of the metal until pouring is completed. The temperature of the metal is raised to and held at 900 C (1650 F) for 5 to 10 min. The crucible is then removed from the furnace and cooled quickly to the pouring temperature.

This process has disadvantages, which may be summarized as follows:

1. Above 800 C (1480 F) the iron pick-up from the crucible may become excessive and the advantages gained by using high purity ingot may be lost, unless extreme care is taken to ensure a good flux layer between the metal and crucible wall.

2. The high temperature required increases fuel consumption and decreases pot and furnace lining life.

3. The time cycle for melting and refining is increased which means lower production from a given installation.

4. Danger of flux inclusions due to poor refining practice.

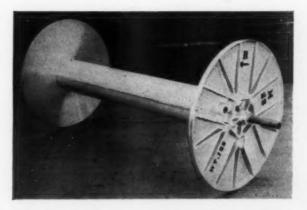


Fig. 3-Magnesium section beam with cast and machined 28-in. flanges and 9-in. drum.

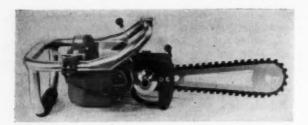


Fig. 4—One-man power saw utilizing several magnesium components.

Carbon Inoculation and Chlorine Treatment—With these obvious disadvantages it was only logical to change to carbon inoculation and chlorine treatments. Considerable work was carried out on these methods by the Dow Chemical Co.,15 the Battelle Memorial Institute,16 Basic Magnesium Inc.,17 and many others, and some of the proposed treatments were tried out in Canadian foundries.

Using a method originated at Basic Magnesium Inc., lampblack was added to the crucible during the melting operation. The total weight of lampblack used was approximately 0.1 to 0.2 per cent of the weight of the charge. About half of this amount was introduced by sprinkling it over the metal at the start of the melting, and the remainder was stirred in after the melting down of the whole charge. Excessive flux tended to agglomerate the lampblack. Refining was carried on as usual, but superheating to 900 C (1650 F) was unnecessary. The lower refining temperatures used, below 800 C (1470 F), ensured maximum effectiveness of the lampblack addition, provided a sufficiently high temperature to pour any casting, and reduced the risk of iron or silicon pick-up. This method worked quite satisfactorily and none of the disadvantages suggested for the method was noted.

Another method, developed by the Battelle Memorial Institute.18 consisted of additions of chlorine and carbon tetrachloride for degassing and grain refining. Production tests were tried using this method, i.e. bubbling chlorine gas through carbon tetrachloride and thence through the magnesium melt. The carbon tetrachloride was maintained at about 27 C (80 F) and approximately 0.02 per cent chlorine per pound of metal was used. This method gave the best results compared to other methods of grain refining and degassing, but it was too complicated for the practical foundryman, who does not have much technical assistance. Foolproof and simple handling methods are of particular concern to the small foundryman, who having been successful in handling aluminum alloys is overwhelmed by such additional difficulties introduced in magnesium founding as complicated fluxing techniques, chlorine degassing, sand inhibitors, etc. In other words, a less tedious and hazardous method of refining the metal, and yet a positive method of producing small grain size, was needed.

At first, a standard refining procedure was adopted including lampblack addition and chlorination of the melt prior to pouring. Later, most of the foundries replaced this procedure by hexachlorethane refining.

Hexachlorethane—Hexachlorethane additions were tried commercially in Canada as early as 1944, but preliminary results were inconsistent. Later, this method was rechecked at the Canadian Bureau of Mines and introduced as standard practice in most Canadian foundries. This method, although not as effective as carbon tetrachloride or lampblack and chlorine treatments, proved successful and eliminated the problems of handling corrosive and toxic chlorine.

The metal is handled the same way as with other methods, but when it has reached the refining stage, tablets averaging 1 to 2 oz in weight are plunged in a phosphorizer at a temperature between 730 to 780 C (1350 to 1440 F), preferably about 750 C (1380 F). There is a vigorous gas evolution and fairly large quantities of dross appear on the surface. The introduction of the tablets requires about 2 min, followed by the normal settling time prior to pouring.

The use of hexachlorethane combines, to some extent, (a) the degassing effect due to the liberation of free chlorine and (b) grain refinement due to carbon inoculation. The amount of hexachlorethane used is normally 3 oz per 100 lb of melt.

Comparative Effectiveness of Various Refining Treatments—To illustrate the effectiveness of the various treatments on the casting soundness and its mechanical properties, Table 8 shows some results of laboratory tests which are self-explanatory. The values given in this table are average results from many experimental melts prepared at the Canadian Bureau of Mines.⁶

Use of organic carbon chlorides in commercial foundries using small melting units provides a convenient, cheap, and dependable method of degassing and grain refining.

Casting—Temperature control is vital in magnesium founding, and particular importance is attached to maintaining accurate controlling equipment.

When the melt reaches the proper pouring tempera-



Fig. 5-"Posuform" desk with magnesium base, desk and seat bracket.

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TABLE 8—EFFECT OF VARIOUS REFINING TREATMENTS ON MECHANICAL PROPERTIES OF AZ80X

| Degassing Method | Grain Refining | Height of Sound Metal,* in. | Grain Size, (0.001 in | U.T.S. .) (kpsi) | | Elong. % on 2-in. |
|---------------------|-----------------------------|--------------------------------------|-----------------------------|---------------------|----------|-------------------------|
| None | None | 2 | 12.5 | 23.4 | 12.3 | 4.1 |
| Solid Flux | Superheating | g 21/2 | 4 | 27.3 | 14.0 | 5.0 |
| Chlorine | Superheating | | 4 | 28.4 | 14.6 | 4.8 |
| Chlorine | Lampblack | 41/2 | 3 | 30.0 | 15.2 | 5.5 |
| Chlorine | CCl ₄ | 41/2 | 3 | 30.6 | 16.1 | 6.5 |
| Hexachlo | rethane | 33/4 | 3 | 29.0 | 15.2 | 5.0 |
| Hexachlo | robenzene | 21/2 | 4 | 29.6 | 14.3 | 5.2 |
| Ferric Ch | nloride | 3/4 | 4 | 30.2 | 15.0 | 6.0 |
| | radiographic Memorial In | | ations of | wedge | castings | as used |

ture, the metal is taken to the molds or the pouring station by crane or buggy. The method of handling depends, of course, upon the foundry and the size of crucible being handled. The flux covering is pushed back from the pouring lip and the metal is dusted during the pouring operation with a mixture of sulphur, boric acid, and fluorides.

Finishing—Castings are sandblasted prior to removing the gates and risers, and finishing is done using the prescribed methods.

Gates and risers are removed by band saws using skip-tooth blades, having 3 to 5 teeth per inch.

After sawing, excess metal is removed by grinding and filing. Flexible shaft grinders and other portable tools (air-chippers, etc.) are used for removing excess material inaccessible to larger grinding wheels. Wet belt sanders are used to advantage.

Normally, all castings are given a chrome-pickle dip prior to shipment.

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Magnesium castings are welded by the "heliarc" method, using either D.C. or A.C. equipment. The smaller foundries use D.C. equipment; the larger foundries have converted to the newer, high-frequency A.C. welding machines, using a stabilizing current super-imposed on the normal welding current. As helium is not readily available in Canada, argon is used exclusively.

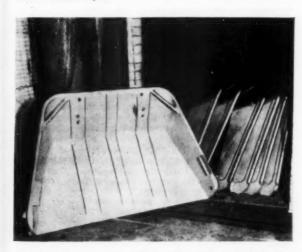


Fig. 6-Magnesium grain shovels replacing fabricated steel shovels.

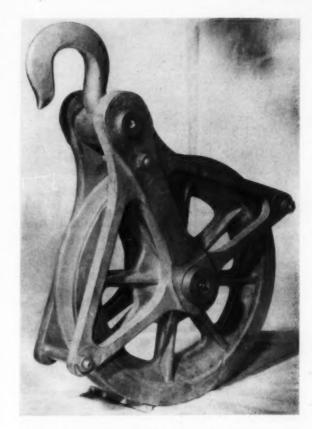


Fig. 7—Diamond drill sheave with cast iron insert in cable groove.

Heat Treatment—Not all foundries have facilities for heat treating magnesium alloys, but those that have are equipped with electrically heated furnaces having circulating atmospheres and temperature control of \pm 5 F.

Surface oxidation during solution heat treatment is prevented by using either sulphur dioxide or pyrite. No protective atmospheres are used during stabilizing or aging treatments.

Because AZ80X or AZ91X alloys are being used predominantly, step heat treatments are not required. These alloys are heated directly to the solution temperature and held from 12 to 48 hr, depending on the section thickness and the alloy composition. AZ91X alloy requires a slightly longer treatment time than does AZ80X for comparable section thickness.

As mentioned earlier, AZ80X is used in the heattreated condition for applications where a high degree of ductility and toughness is required. AZ91X is used in the HTA condition where high yield strengths are desired.

Aging treatments are carried out at 150 to 220 C (300 to 425 F) for 12 to 16 hrs.

Stabilizing treatments 2 to 6 hr at 220 to 290 C (425 to 550 F) are given to the castings, in some cases, to relieve internal stresses and to ensure dimensional stability.

Inspection—Foundries producing aircraft castings are, of necessity, equipped to handle the radiographic

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Fig. 8-Clicker arm.

examination of castings. X-ray equipment in use varies from 90 KVA to 220 KVA units. The 90 KVA units are usually housed in self-contained lead-lined cubicles and are used for relatively small and light-sectioned castings. The larger 150 KVA and 220 KVA units are movable and set up in screened rooms to allow the larger castings to be more easily handled during the examination.

Tensile testing equipment is available for determining mechanical properties as required for quality castings.

Costs

The basic cost factors in the casting industry are the cost of the metal and the cost of fabricating it into usable form. Canada has surplus magnesium production at a cost comparable with that of other countries. Magnesium castings are in direct competition with aluminum castings, and records to date indicate that the industry is competing favorably on a cost basis on high quality work and larger production runs such as of match plate castings.

All magnesium castings are sold on a per piece or volume basis.

Applications

It is impossible in this paper to discuss the numerous applications of magnesium sand castings developed and used in Canada. Some of these applications are similar to American and British products, but local conditions, small population, and the rather early development stage of our industry have produced specific Canadian design. A few applications which have been successful are included because of their importance in development of the industry in Canada. In the case of the section beam, a cast and machined drum has been necessitated because wrought products were not available.

The brightest domestic future for magnesium appears to lie in the fields of transportation and material handling. Canada is a young country, thinly populated, and with immense distances to bridge. These conditions must foster light weight equipment.

As in other countries, further growth of the Canadian magnesium casting industry is dependent upon development of new applications and improvements of foundry techniques. To facilitate research in this field, an Industrial Advisory Committee on Magnesium Research was formed, in 1945, at the Canadian

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Bureau of Mines, including representatives from the magnesium producing and consuming industries, from the Canadian Armed Forces, from universities, and from research organizations. Aims of this Committee are to discuss research projects carried out on magnesium alloys at the Canadian Bureau of Mines or in other research laboratories and to suggest new investigations on problems encountered in fabrication or service of magnesium alloy products.

The larger companies are also members of the Magnesium Association in New York, getting the benefit of close contacts with the magnesium industry in the United States.

It is only by this close cooperation in the magnesium industry and the realization of the importance of reduced foundry costs that successful improvement of more efficient foundry techniques will be assured.

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DISCUSSION

- Chairman: M. E. BROOKS, The Dow Chemical Co., Bay City,
- Co-Chairman: E. J. VARGO, Wellman Bronze & Aluminum Co., Cleveland.
- C. E. NELSON (Written Discussion): 1 My remarks regarding

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this excellent paper are intended to be interpretive and should not be construed to be critical of this paper in any sense.

In the authors' considerations on alloys the point is made to the effect of starting with and maintaining high purity metal in

terms of iron, nickel, copper, etc. Speaking as one of the authors of the original corrosion paper10 which pointed out the effect of these elements on corrosion in salt water immersion, I can truthfully say that we may have done more to confuse the issue than we have accomplished constructively. This comes about because of the fact, established by both commercial use and controlled testing of castings that these minor impurities have no significant effect on atmospheric or normal service corrosion and their effect is limited to uses or conditions where the parts are subject to substantial exposure to actual immersion in, or contact with saline solutions. It is this misconception plus the fact that the 2 and 3 per cent zinc containing alloys nullify the bad effect of minor impurities tested in salt solutions but add nothing to improvement to ordinary atmospheric exposure that has interfered with a wider usage of the low-zinc, Mg-Al-Mn alloys in the United States. It has been extremely difficult to convince buyers and specifying agencies that some of the advantages of the low-zinc alloys for many uses outweigh any chance gain in corrosion resistance imparted by the higher Zn. I think it is safe to say that this situation is changing and that greater usage of the low-zinc, Mg-Al-Mn alloys will be forthcoming in the United States.

A further important observation is that if one expects or encounters service conditions that are corrosive to magnesium then it is many times better insurance to rely on a good paint schedule than on the difference due to either high purity or high-zinc alloys.

As a second point I would like to advise strongly that SO₂ in the amounts of I per cent or above be used in solution heat treating all magnesium casting alloys.

With regard to sand protecting agents I am certain that some American foundries are also using only sulphur and boric acid as inhibitors although there is variability between foundries regarding the use of additional agents such as fluorides.

Finally, I would like to commend the full and valuable interchange of information between the Canadian and American workers that has been experienced and is further evidenced by the valuable papers presented at this meeting.

MR. MEIER (Reply to Mr. Nelson): I want to thank Mr. Nelson for his kind comments and his discussion. I agree that the problem of corrosion of magnesium alloys, especially of those with higher zinc contents, is in many applications of magnesium alloy castings not as important as it is often stated. I believe also that a good painting job or any other of the excellent surface treatments is much more important than the corrosion resistance of the material itself. But there are also many applications where the use of high-purity magnesium is considered essential

It has been claimed recently that the use of highest-purity magnesium in casting alloys is impractical because of the considerable iron pick-up during the remelting and refining in steel crucibles. This is not borne out in Canadian foundry practice, where the iron and silicon pick-up under proper foundry techniques is negligible. To illustrate this statement some chemical analyses of laboratory and commercial melts of various magnesium alloys are given in Table 9 and Fig. 9.

Table 9 shows the analyses of 195 experimental melts prepared at the Canadian Bureau of Mines. The rather high deviations of aluminum, zinc, and manganese contents were due to variations in the composition of some experimental alloys used. The averages of impurities found are remarkably low in spite of the fact that many of these melts were superheated for grain refinement. All melts were prepared in steel crucibles of 60 to 90-lb capacity. Specimens for analyses were taken from sand castings.

·Figure 9 shows the distribution of iron and silicon contents in 90 commercial foundry melts of magnesium alloy AZ80X sand castings. These values are quoted from analyses of sand castings taken daily from the current production of commercial castings during a statistical survey of casting quality in a Canadian magnesium foundry. It is obvious that the impurity range

² Technical Director, Magnesium Div., The Dow Chemical Co., Midland, Mich.

TABLE 9-ANALYSES OF EXPERIMENTAL MELTS

| Alloy Designation | AZ80X | AZ63X | AZ92X | AZ91X |
|-----------------------|------------|------------|---------|---------|
| Nominal Composition: | % | % | 01/0 | % |
| Aluminum | 8.0 | 6.0 | 9.0 | 9.5 |
| Zinc | 0.4 | 3.0 | 2.0 | 0.5 |
| Manganese | 0.3 | 0.3 | 0.3 | 0.3 |
| Actual Analyses | | | | |
| No. of melts analyzed | (120) | (30) | (35) | (10) |
| AluminumMax | 8.75 | 6.27 | 9.18 | 9.94 |
| Min | 7.41 | 5.55 | 8.65 | 9.03 |
| Ave | 8.08 | 5.85 | 8.99 | 9.45 |
| ZincMax | 0.65 | 3.31 | 2.22 | 0.60 |
| Min | 0.28 | 2.80 | 1.90 | 0.35 |
| Ave | 0.42 | 3.05 | 2.05 | 0.47 |
| ManganeseMax | 0.45 | 0.51 | 0.47 | 0.56 |
| Min | 0.24 | 0.20 | 0.25 | 0.22 |
| Ave | 0.33 | 0.38 | 0.34 | 0.30 |
| IronMax | 0.007 | 0.003 | 0.004 | 0.005 |
| Min | < 0.001 | < 0.001 | < 0.001 | < 0.001 |
| Ave | 0.0014 | 0.0013 | 0.0015 | 0.002 |
| SiliconMax | 0.035 | 0.016 | 0.022 | 0.030 |
| Min | 0.002 | 0.004 | 0.004 | 0.006 |
| Ave | 0.012 | 0.008 | 0.013 | 0.013 |
| Copper Max | 0.004 | 0.004 | 0.004 | 0.002 |
| Min | < 0.001 | < 0.001 | < 0.001 | 0.001 |
| Ave | 0.002 | 0.0018 | 0.0022 | 0.0014 |
| DETERMINATION: Chemic | al-Al, Zn | , Mn | | |
| Spectro | graphical- | -Fe, Si, C | u | |

DISTRIBUTION OF IRON AND SILICON CONTENTS
IN 90 COMMERCIAL FOUNDRY MELTS OF MAGNESIUM

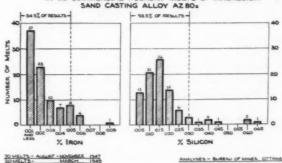


Fig. 9

under commercial foundry conditions cannot be as carefully controlled as in the laboratory, but the values shown indicate that the iron and silicon pick-up can be kept very low.

We agree also with the remarks on the advisability of using sulphur dioxide as protective atmosphere in the solution heat treatment of magnesium casting alloys. Unfortunately, in most Canadian foundries the heat treating facilities are being used for both magnesium and aluminum alloy castings and this limits the possibility of using sulphur dioxide. In our laboratory work we are using, since more than five years, carbon dioxide as protective atmosphere and have never had any difficulties. As far as I know, in Europe no one is using sulphur dioxide protection in heat treating of magnesium alloys. But, I agree that the use of sulphur dioxide is generally safer.

Mr. Nelson: We thoroughly agree with the authors that these alloys can be made of high purity and that they can be handled in the foundry. I think the best evidence of that is that we have made millions of pounds of magnesium wrought products of controlled high purity using commercial alloying materials and electrolytic magnesium. In the case of castings we have concluded that there is no need or service advantage of going to the higher purity grades.

I might add that the German practice, as well as that of the British has been to use these magnesium-aluminum casting alloys with little or no zinc, without controlling purity, and as

far as we have been able to find out, they have not experienced any significant corrosion trouble.

F. P. STRIETER: 2 The authors mentioned thermic risers. Would they care to explain the subject in more detail especially in regard to the extent of their use in Canada?

MR. MARTINSON: We tried successfully to use exothermic risers on large aircraft castings. It would be too early to assess the advantages and limitations of this technique.

I would add also to the remarks of Mr. Nelson that I agree entirely with his opinion on the value of protective surface treatments but British and Canadian aircraft specifications call for high-purity magnesium alloy castings with an iron content limited to 0.002 per cent. It would be difficult to argue that a painted or otherwise coated magnesium alloy casting is still safer from the corrosion point of view if it is made from highpurity magnesium. I can see no advantage in buying lower grade magnesium if you can buy the high-purity metal for the same price.

R. E. WARD: 3 The figures on the use of chlorine with carbon tetrachloride, as I recall, showed the best properties. You mentioned that although it did show good results, it had not been widely used because the small foundries did not have adequate facilities. I recall there has been some difficulty in actually controlling the use of that combination. Did you use that at any time and find that the control was quite critical?

MR. MARTINSON: Carbon tetrachloride was used in commercial work only long enough to find out whether it would be satisfactory. However, as you mentioned, you had to have an engineer on the job to see that it was done properly or you could not get good results. In small foundries, as we have them in Canada, this would be impractical.

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FORMATION OF TECHNICAL MANAGEMENT GROUPS FOR FRENCH FOUNDRY INDUSTRY

By

M. Jean Lain*

AFTER WORLD WAR I the problem of creation of a specialized management group for French foundries presented itself at the moment when the industry began its marked development.

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Professional foundrymen attacked this important problem, thanks to the initiative of the eminent engineer, Eugène Ronceray, well known in France and abroad.

Soon a Foundry High School (Ecole Supérieure de Fonderie) was established for the purpose of educating a staff of foundry technicians. This school was immediately approved by the government and placed under the authority of the General Foundry Syndicate (Syndicat Général Des Fondeurs de France).

History

In his "Report on Apprenticeship," presented at the Foundry Congress at Liège (Sept. 28–Oct. 1, 1921), Mr. E. Ronceray presented his recommendations to facilitate recruitment of apprentices. In the same report Mr. Ronceray requested opening of a Foundry High School "for people already acquainted and having experience in the profession." This report consolidated the ideas voiced later by M. D. Waeles in Fonderie, No. 26, Feb., 1928 and his exchange paper published by The Institute of British Foundrymen, June 14–17, 1949.

The idea of the creation of a school planted here took shape rapidly.

The General Foundry Syndicate at their meeting on July 30, 1923, with the cooperation and approval of the government, created the Foundry High School with its purpose to form a group of competent engineers and foundry managers with sufficient knowledge of metallurgy, equipment and organization to allow

them to exercise their profession intelligently. The assigned mission was "Best Production at Lowest Cost."

The General Foundry Syndicate took immediate care of the organization of the school administration and management, and the first lectures were given under the personal guidance of Mr. E. Ronceray.

Beside Mr. Ronceray three other prominent persons were active in this preliminary work: Mr. Edmond Labre, Director of Technical Education, Mr. Charles Defour, President of General Foundry Syndicate, and Mr. Hippolyte Luc, Attached Director of Technical Education.

Necessary funds were furnished, one half by the Department of Technical Education, the other half by the General Foundry Syndicate. The school was officially approved by government decree June 30, 1925 and the inauguration took place during the International Foundry Exposition in Paris (Sept. 7–10, 1927) in presence of numerous representatives of French and foreign foundrymen.

Number of Students

During its 26 years of existence, the school has admitted 604 students among them 62 foreigners of 24 nationalities: Argentine, Belgium, Brazil, Bulgaria, Chile, Czechoslovakia, Denmark, Egypt, Great Britain, Greece, Italy, Japan, Liban, Lithuania, Luxenburg, Norway, Palestine, Poland, Portugal, Spain, Sweden, Switzerland, Syria, and Turkey. This shows how famous the school has become in foreign countries.

Taking into consideration the fact that the school was closed for two years during World War II, the number of graduates per year is an average of 25 including 2 to 3 from foreign countries.

Quantity of graduates differs according to the demand of the industry. Before 1939 the average graduation amounted to 20 pupils. It is to be noted that during the period 1924 to 1939 the maximum was 29, this in 1926, and the minimum of 9 in 1936.

Since 1945 the average of graduates has increased to 40 as a consequence of the increased demand of the industry, the modernization of foundry equipment, and general progress in the foundry field.

Ing. E.C.P. and E.S.F., Dir. of L'Ecole Supéricure de Fonderie, Vice-Pres. of L'Association Technique de Fonderie de France.

Official exchange paper from the French Foundry Technical Association presented in abstract form at the 54th Annual Meeting of the American Foundrymen's Society, May 8-12, 1950, Cleveland.

Translated from the original French by H. W. Zimnawoda through the courtesy of National Engineering Co., Chicago, where Mr. Zimnawoda is Export Manager.

It is assumed that in the future the number of graduates will average 20 to 30; this amount will satisfy the normal requirement of the French foundry industry and be slightly higher than the prewar demand.

Recruiting

The French students entering the school are between 20 and 30 years of age. Either engineers graduated from the schools approved by the Ministry of National Education, or people with foundry shop experience and theoretical knowledge of the foundry processes. The management of the school determines the maximum of admissions for each year according to the needs of the foundry industry. Because this is a special type of school, there is no reason for graduation of more students than can be used in the industry each year.

The counsel of the management, which consists of well-informed persons, has fixed the number of 30 as

the maximum of French graduates.

Whatever the origin of the student may be, it is required that he have previous foundry experience. The ideal student would be one who has occupied a certain position in the foundry industry for a certain period of time and who comes to this school to fill the gaps in his education and to perfect his knowledge.

Foreign students are recruited according to the same rules. However some facilities in the rules are made regarding their age or previous foundry experience. They must have a complete knowledge of the French language to gain full benefit of the courses, and their diplomas must be approved by the Ministry of Education.

Free students, French or foreign, may be admitted after confirmation of their position in the foundry and if registration has not been closed because the school's quota was filled. They are usually of more advanced age. They follow the entire course or part of it depending upon their needs.

Selection of Candidates and Course

The course lasts no more than one year (Oct. to mid-July). It would be illusory in such a short period of time to try to specialize the high ranks of executive foundry technicians who would be capable of serving the foundry industry immediately after leaving the school. Therefore the young men have to be well prepared to get full benefit of the acquired knowledge.

This is the reason that their education starts with practice in the shop before entering the school.

On the other hand the foundry industry requires that candidates for management positions possess knowledge of physics, chemistry, metallurgy, etc., and their physical and moral conditions must be satisfactory.

The first selection is made during their industrial practice when it can be seen whether or not they are

following the right vocation.

The second selection is made by admission examination. The students should know the principles of steel and iron production in blast furnaces, openhearth furnaces, electric furnaces and convertors. They also should have basic information of nonferrous metallurgy, sand preparation, patternmaking, molding, coremaking, and cleaning of the castings.

Industrial Training

The purposes of this training are multiple. First of all, it is important that the future graduate becomes acquainted with the rudiments and the language used in the trade; then that he become familiar with industrial ambient in which he will be called on to work; further he should understand the role of coordination between different sections of the foundry, be aware of the required qualities of personnel, develop the ability of observation, ascertain his sense of criticism; and finally he must get a feeling and understanding of industrial problems which will confront him.

This industrial training is organized, directed and controlled by the management of the school. Principally the student begins his work in a gray iron foundry using the most popular types of patterns and getting familiarity with molding and pouring procedures.

The program includes visitation of foundries with detailed inspection of all departments: sand preparing plant, coreroom, molding section, mold drying ovens, melting department, cleaning room etc. The following are also included:

Study of pattern rigging and molding method with cooperation of professional molders.

Execution of some simple molds under supervision of professional molder.

Detailed study of different types of patterns, simple and complicated and construction of core boxes.

Analysis of a molding system on the basis of a submitted drawing including approximate weight of casting, addition of material for machining, shrinkage problem due to the existence of cores, core box construction and all operations pertaining to the production of castings.

While this first part of the program deals principally with molding, the second part acquaints the student with application of alloys in each type of foundry.

The student must make detailed notes about what he has seen and done. These notes are of great value and will be used in his future professional career.

Length of the individual training must be a minimum of 12 months for candidates having engineers degree. For the others, the time is longer—up to 18 months.

The preparation for the examinations for admission is conducted in special courses organized by the General Foundry Syndicate.

In reply to the appeal of Maurice Olivier, Executive President of General Foundry Syndicate, more than 150 foundries, dispersed all over the country, have agreed with enthusiasm to receive into their shops students of the foundry school.

Teaching Program of Foundry High School

The program deals exclusively with the foundry, its particular problems and methods of production; it does not have an encyclopedic character. In line with the indications given by Mr. Ronceray, first director of the school, the staff of the professors, about 30 of them, conducts their lectures by teaching the practical approach to their subjects. Professor Albert Portevin, the famous scientist, who succeeded Mr. Ronceray as director of the school, himself teaches the course on the constitution and structure of metals and their heat treatment.

Besides the courses directly related to the foundry proper, other subjects are taught: industrial heating, metallography, mechanics, electricity, surface treatments, etc.

To complete the education of the pupils the following subjects are taught at the end of the course: factory layouts, price estimates, organization, planning of production, laboratory work, control and inspection, and method for prevention of accidents. In the school the students are also taught details of the function of the foundry manager, patent procedures and use of foundry publications.

Once or twice each week the students visit foundries located in the vicinity of the school. These visits are described by the pupils in their reports. These reports are of great value, for they develop the students ability to observe the details of foundry operations.

Besides the 20 oral examinations, the students prepare during their year of study a certain number of written assignments. Studies of patterns and molding systems are supplemented by sketches or drawings. Project layouts and organization of work in individual sections of the foundry are some of the topics of these reports. All of these reports and surveys are discussed by the students divided into groups. Each group receives a certain subject to deal with.

Confirmation of Diplomas

In adultion to all previously mentioned assignments and examinations, the students at the end of the course are given a final examination before a jury presided over by the General Inspector of the Department of Technical Education and all members of professional staff.

The final examinations deal with the entire program of the course: study of a given molding problem on the basis of a submitted drawing, and project of the foundry with layout of the equipment involved.

The graduate engineers who desire to continue their study in the research field may obtain a degree of Doctor-Engineer at the university.

Positions Occupied in Industry

The administration of the school fixing the maximum amount of admissions for each year acts according to the demands of the industry. As a result thereof each graduate student obtains a job. As a matter of fact, since the creation of the school there was no case where a graduate engineer or technician after finishing the school did not find a job in line with his ability

and qualification.

An Alumni Association collects the offers for situations from the industry and informs the recent graduates about the vacant positions.

The majority of pupils (80 per cent approximately) goes to the independent foundries, that means foundries producing raw or partly machined castings. The education and practical experience received during the study allows the students to take these jobs and select the type of foundry most suitable for them.

Some of the graduates (15 per cent approximately) sent to the school by companies which employes them, return to their previous positions.

Among these companies are the factories under government administration such as, railroads, navy, air force, arsenals, etc., and others specialized in certain types of castings for automobiles, machine industry, farm implements, etc.

Finally a small number (5 per cent approximately) direct themselves according to their individual taste to the laboratories and research work.

The young engineer starts his career as assistant to Production Manager, to be in the near future a Production Manager himself, and finally a Foundry Manager or sometimes to start his own foundry. Actual statistics show the figures of job distribution as follows:

| Plant engineers | 38 | per | cent |
|-------------------------------------|----|-----|------|
| Production managers | 32 | | 93 |
| Foundry managers and foundry owners | 23 | 25 | 33 |
| Laboratories and research | 5 | 9.9 | ** |
| Other positions | 2 | | ** |

These statistics show that only a small number of graduates abandon the foundry field for which they were trained.

Conclusion

In the limit of this paper were sketched the characteristic features with regard to the formation of high ranks of technical personnel. Thanks to the cooperation of General Foundry Syndicate and the Department of Ministry of National Education this formation is based on the demands of the industry and the diplomas of graduate students are approved by the Ministry of Education.

The performance of this task, conducted for 26 years by the Foundry High School, assures that the graduate engineers have received adequate education and mechanical training to go into the foundry field with complete knowledge of their functions.

Graduate engineer technicians can complete their education by the study of technical magazines published by the Foundry Association and Technical Center of Foundry Industries and participating in the monthly meetings and National and International Congresses organized by the Foundry Association.

DEVELOPMENT OF HIGH-STRENGTH MAGNESIUM CASTING ALLOY ZK61

By

J. W. Meier* and M. W. Martinson**

ONE OF THE MOST IMPORTANT AIMS of magnesium alloy research is the creation and development of alloys with increased strength-to-weight ratio showing reasonable corrosion resistance and good workability. Unfortunately, only slight progress has been made in the commercial use of any new alloys other than the long established standard alloys of the Mg-Mn and the Mg-Al-Zn type.

Extensive research work on new alloys or on new alloying additions to standard alloys was conducted mostly without practical results. One of these studies, started in 1937 by Sauerwald¹ (Germany), showed that zirconium additions to magnesium and some magnesium alloys produced considerable grain refinement and indicated promising possibilities of improvement of mechanical properties and forming characteristics of these alloys.

Further investigations on the magnesium-zirconium and the magnesium-zirc-zirconium alloy systems were continued in Germany²⁻⁵ and in Great Britain⁶⁻⁸ to overcome difficulties in the effective introduction of zirconium into magnesium alloys, and to establish proper fabrication techniques and heat treating cycles for these alloys. As reported in 1946 by Major Ball of the British Magnesium Elektron Ltd., his firm overcame successfully most of these difficulties and introduced commercially zirconium containing magnesium casting alloys and wrought products.

In the United States the Dow Chemical Co. reported in 1946 results of extensive research on Mg-Zn-Zr alloy extrusions⁹ and made these products available

commercially.

Most of the above work was carried out on wrought products because of the good formability and the excellent mechanical properties of these alloys. As reported, they can be easily rolled, extruded, forged, etc., and show corrosion resistance comparable to magnesium-manganese alloys. Published data on these alloys show ultimate tensile strengths up to 58,000 psi; tensile yield strengths up to 47,000 psi; compressive yield strengths up to 42,000 psi; and elongations from 10 to 15 per cent. Considering the strength-to-weight ratio these alloys are equal to the highest-strength Al-Zn-Mg alloys of the 75S-T type.

Mg-Zn-Zr Casting Alloys

In the sand casting field the Germans¹⁰ originated the "Elektron ZB" alloy, containing 3 per cent zinc and 0.5 per cent zirconium, and the British introduced "Elektron Z5Z" alloy with 4.5 per cent zirc and 0.7 per cent zirconium. Typical mechanical properties published for these alloys are shown in Table 1 which includes, for comparison, also typical results for alloy ZK61 achieved at the Canadian Bureau of Mines.¹¹ Properties of these alloys after heat treatment are included in Table 6.

TABLE 1—DEVELOPMENT OF CAST MG-ZN-ZR ALLOYS (Typical Ranges of As-Cast Properties)

| | Nomin | al Comp. | | | | |
|------------|-------|----------|--------------|------------------|------------------|----------------------|
| Alloy | Zn, % | Zr, % | UTS, kpsi | 0.2% PS, kpsi | 0.1% PS, kpsi | Elong. % on 2 in. |
| ZB | 3 | 0.5 | 25-34 | 10-14 | _ | 5-15 |
| Z5Z | 4.5 | 0.7 | 35-37 | - | 18-19 | 8-10 |
| ZK61 | 6 | 0.8 | 38-42 | 20-22 | 18-20 | 8-14 |

Establishing in 1945 its broad research program on magnesium alloys, 12 the Canadian Bureau of Mines included a study of zirconium-containing casting alloys and an extensive foundry investigation was started in early 1946.

About 250 experimental melts, up to 90 lb in size, were prepared from binary, ternary and quaternary alloys of magnesium with zirconium, zinc, cadmium, calcium, copper, cerium, and beryllium. A special investigation was carried out on experimental heat treatments to obtain maximum properties for these alloys.

The range of experimental alloys prepared to date included binary Mg-Zr alloys up to 0.7 per cent; ternary Mg-Zn-Zr alloys with zinc contents of 1 to 11 per

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cent and Zr up to 1.2 per cent; some ternary alloys of the systems Mg-Cd-Zr, Mg-Cu-Zr, Mg-Ce-Zr, Mg-Zr-Ca, Mg-Zr-Be; and some quaternary alloys of Mg-Zn-Zr with additions of Be, Ca, Cd, Ce and Cu.

The present paper presents some of the results achieved, dealing mainly with the most outstanding alloy of this group, i.e. alloy ZK61, a magnesium casting alloy containing 6 per cent zinc and over 0.7 per cent zirconium.

Materials and Experimental Procedures

For the preparation of the experimental alloys the following materials were used:

High purity magnesium as supplied by Dominion Magnesium Limited (approx. 99.98 per cent), electrolytic zinc (99.99 per cent), electrolytic cadmium (99.99 per cent), electrolytic copper (99.9 per cent), Domal high purity calcium (99.5 per cent), and Mischmetal (approx. 50 per cent Ce, 50 per cent other rare earths, and less than 1 per cent Fe).

Zirconium was introduced, in most cases, as a fused salt mixture containing approximately 50 per cent zirconium tetrachloride and 25 per cent each of sodium and potassium chlorides. In some of the experiments zirconium was introduced in the form of one of the following: pure zirconium powder, zirconium tetrachloride, zirconium fluoride, potassium fluozirconate, zirconium hydride, lithium zirconate, and binary or ternary zirconium containing master alloys.

The melts were prepared in welded iron pots of 60

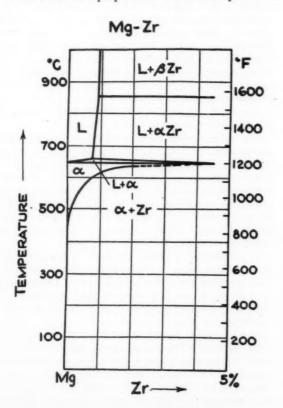


Fig. 1—Partial equilibrium diagram of Mg-Zr (Sauerwald).

to 90 lb capacity, using Domal crucible flux (Dow 310 type). Temperatures during melting and alloying operations were held, whenever possible, under 800 C (1470 F) and pouring temperatures between 750 to 780 C (1380 to 1435 F). The alloying elements were stirred in thoroughly to ensure uniform alloy compositions. No chlorination after alloying additions and no grain refining by superheating or carbon inoculation treatments were used.

All castings were cast into green sand using standard sand composition and characteristics for magnesium alloys.¹³ The Dow Chemical Company's test bar mold design¹⁸ was used in the whole investigation, as well as fluidity spirals and wedge castings for evaluation of microporosity.¹⁸ Tensile properties reported in this paper were obtained on cast-to-shape test bars without machining.

To check results obtained on test bars some commercial castings also were produced. These commercial castings were tested using small specimens cut out from various sections. The results of these tests were surprisingly high and showed great uniformity of the alloy throughout the castings.

All heat treatments above 300 C (570 F) were carried out in circulating-air furnaces, heated electrically and controlled to \pm 3 C. Carbon dioxide, supplied in bottles, was introduced into the furnace as protective atmosphere. Aging treatments below 300 C (570 F) were carried out in an electric oven with air circulation and close temperature control (to \pm 1 C) without using protective atmospheres.

Zirconium Content

Earlier work on zirconium additions to magnesium and some magnesium-base alloys showed their beneficial effect on grain refining and considerable improvement of mechanical properties. It was also reported that zirconium additions are not successful in magnesium alloys containing aluminum, antimony, manganese, nickel, silicon, or hydrogen. These "objectionable" elements form with zirconium in the liquid phase intermetallic compounds insoluble in magnesium.

The solubility of zirconium in magnesium was investigated by Sauerwald, Siebel, and Nowotny, Wormnes and Mohrnheim. Figure 1 shows the "preliminary" equilibrium diagram for Mg-Zr established by Sauerwald, which reveals limited solubility of zirconium in magnesium. It was shown that zinc additions to Mg-Zr alloys increase, to some extent, the solubility limit of zirconium. Stroup and Sager found that additions of zirconium are effective in purifying magnesium by removing of excessive iron (see Table 2).

In actual foundry practice it was established that the "effective" zirconium solubility in binary Mg-Zr alloys is approximately 0.65 per cent and in Mg-6Zn-Zr alloys approximately 0.80 to 0.85 per cent.

As reported by Ball⁶ and Sauerwald² the grain refining effect depends on the amount of zirconium which is actually in solution in the magnesium. This "effective" zirconium content is directly related to the grain size and the mechanical properties in the as-cast condition as well as after heat treatment. Excessive or

TABLE 2—CHEMICAL COMPOSITIONS AT VARIOUS STAGES OF ALLOYING (ZK61 alloy-50 lb melts)

| Mel | No. | | A | 1 | В | | С | | D | Alloying |
|--------|-----------|------|--------|------|--------|------|--------|------|--------|--|
| Compos | sition, % | Zr | Fe | Zr | Fe | Zr | Fe | Zr | Fe | Stage |
| Sample | No. 1 | | 0.0218 | | 0.0020 | | 0.021 | | 0.0016 | Pure Mg melt |
| ** | No. 2 | 0.15 | 0.0115 | 0.20 | 0.0044 | 0.14 | 0.0042 | 0.22 | 0.0035 | After addition of 1 lb salt mixture |
| ** | No. 3 | 0.33 | 0.0077 | 0.33 | 0.0049 | 0.31 | 0.0018 | 0.45 | 0.0042 | 2 lb-salt mixture |
| ** | No. 4 | 0.60 | 0.0074 | 0.66 | 0.0057 | 0.58 | 0.0014 | 0.66 | 0.0019 | 4 lb salt mixture |
| ** | No. 5 | 0.72 | 0.0065 | 0.72 | 0.0034 | 0.64 | 0.0019 | 0.68 | 0.0025 | 6 lb salt mixture |
| ** | No. 6 | 0.74 | 0.0035 | 0.70 | 0.0026 | 0.61 | 0.0014 | 0.69 | 0.0025 | After stirring in fo |
| ** | No. 7 | 0.73 | 0.0021 | 0.70 | 0.0030 | 0.65 | 0.0017 | 0.67 | 0.0017 | 4 min |
| ,, | No. 8 | 0.97 | 0.0020 | 0.92 | 0.0025 | 0.89 | 0.0013 | 0.91 | 0.0015 | After addition of 6 per cent zine |
| | No. 9 | 0.90 | 0.0014 | 0.84 | 0.0014 | 0.78 | 0.0005 | 0.83 | 0.0006 | After settling and pouring |

"free" zirconium remains in varying amounts in finely divided particles dispersed throughout the alloy.

Figure 2 shows the relation of zirconium contents on the tensile properties of binary Mg-Zr alloys and the ternary Mg-6 per cent Zn-Zr alloys, as obtained on experimental melts at the Canadian Bureau of Mines. The values plotted on the graph are not individual results but average results for particular melts, and are based on 5 to 10 tests.

It was found that maximum mechanical properties can be achieved only if the "effective" zirconium content is well over 0.7 per cent and the average grain size is in the range of 0.001 to 0.002 in., in any case, below 0.003 in. For the upper range of zirconium additions to the Mg-6 per cent alloy, the graphs show two lines. The lower line (dotted) is based on results obtained in the first stage of the investigation. Later results showed considerable increases in tensile properties for alloys containing 0.7 to 0.85 per cent zirconium (full line), and also more consistent grain size. These improvements were attributed to better alloying techniques and consequent increased "effective" zirconium content.

The diagram in Fig. 2 shows that optimum mechanical properties of alloy ZK61 are obtained only if the zirconium content is well above 0.7 per cent. Since chemical determinations of "soluble" and "free" zir-

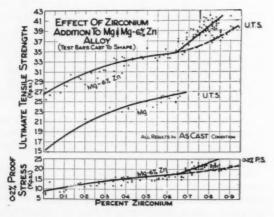


Fig. 2-Effect of zirconium addition to magnesium and Mg-6Zn alloys.

conium are not consistent, the diagram was based on "total" zirconium contents. This caused the apparent disagreement of results on the dotted and the full lines. It shows, also, that the "total" zirconium content may be misleading if it is not correlated with grain sizes and mechanical results.

Alloying Technique

As mentioned already, in most cases zirconium was introduced into the molten magnesium as a fused chloride mixture. The melting and alloying procedure found to give the most consistent results, is as follows:

Melt down magnesium ingots under crucible flux cover and raise temperature to 770 to 780 C (1420 to 1435 F). Add required amount of zirconium tetrachloride fused salt mixture and stir thoroughly for at least 10 min. Add zinc (at 770 to 780 C) and stir again thoroughly. Hold melt under covering flux sufficiently long to ensure settling (10 to 15 min). Cool to casting temperature and pour.

Successful production of ZK61 castings, using the above method, depends on

a. Careful attention to the fused salt mixture during storage.

b. Good fluxing technique during alloying of the fused salt mixture. It is believed that high "effective" zirconium contents can be obtained only if a proper technique of stirring in of the salt mixture is established. Best results for small melts (up to 90 lb) were attained using manual stirring; for larger melts mechanical stirring is necessary.

c. Although it is known that zinc increases the solubility of zirconium in magnesium, experience to date indicates that the addition of zinc after the introduction of zirconium gives more consistent results.

d. Very careful handling of metal during the casting operation.

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To illustrate the effect of the various stages of alloying, Table 2 shows analyses of samples taken from four 50 lb melts of alloy ZK61 during the alloying operation. To explore the effect of iron content, in two of the melts (A and C) electrolytic magnesium was used and the other two melts (B and D) were prepared using Domal high-purity magnesium.

The analyses illustrate clearly:

a. Effect of zirconium on the reduction of iron contents.

b. Effect of zinc on increasing the solubility of zir-

Table 3 shows the effect of the pouring temperature on mechanical properties of as-cast and fully heat treated ZK61 alloy. Although the values for this melt show that variations in the pouring temperatures above 700 C (1290 F) had not much effect on the tensile properties, experience to date shows that the most consistent results were achieved using pouring temperatures in the range of 750 to 780 C (1380 to 1435 F).

TABLE 3-EFFECT OF POURING TEMPERATURE
Melt No. 1556-Alloy: ZK61
(Each value is an average of three results)

| Pouring | | As-cast | | Heat | Treated a | nd Aged |
|-----------|-------------|-----------------|-------------------|-------------|-----------------|-------------------|
| Temp., °C | UTS kpsi | 0.2% PS kpsi | Elong. % on 2 in. | UTS kpsi | 0.2% PS kpsi | Elong. % on 2 in. |
| 800 | 39.2 | 21.4 | 8.0 | 44.9 | 29.8 | 6.0 |
| 780 | 40.1 | 22.3 | 9.5 | 47.1 | 30.0 | 10.0 |
| 760 | 40.5 | 22.5 | 9.2 | 47.4 | 29.3 | 9.8 |
| 740 | 39.0 | 21.9 | 8.0 | 46.5 | 29.3 | 10.5 |
| 720 | 40.2 | 21.9 | 10.5 | 47.5 | 29.3 | 12.0 |
| 700 | 38.6 | 21.6 | 8.0 | 46.0 | 30.2 | 10.0 |
| 680 | 36.0 | 21.6 | 6.0 | 42.6 | 29.5 | 6.0 |

Experiments on remelting ZK61 alloy scrap showed that the loss in zirconium content can be held low, but in commercial foundry conditions it is necessary to add, during the remelting of scrap or ingots, additional amounts of zirconium salt mixture. This provides, also, some refining and degassing of the melt.

Efficiency of zirconium additions, using the zirconium tetrachloride fused salt mixture, varied considerably, as would be expected in experimental work with changing alloy compositions. Average efficiency of zirconium in alloy ZK61 was approximately 35 per cent, which is slightly higher than that reported by Doan and Ansel.9

Other methods of alloying zirconium with magnesium were tried and included various master alloys and zirconium compounds. In some of the experiments these methods produced good results, although they were not so consistent as the chloride mixture or alloying methods combining other compounds with at least some amount of the chloride mixture. It is believed that the exceptionally high properties obtained using the chloride mixture is partially due to the efficient refining and degassing action of the liberated chlorine. Especially important seems to be the removal of hydrogen which affects, according to Sauerwald, greatly the solubility of zirconium in magnesium and magnesium alloys.

Emley^{17,18} states, in his excellent papers on nonmetallic inclusions in magnesium-base alloys, that the use of alkali metal chlorozirconates in the preparation of zirconium-containing magnesium alloys causes contamination of the castings with flux inclusions. These flux inclusions were found to be nonmetallic products of chemical reaction (MgCl₂.NaCl), entangled in zirconium-rich particles and thereby prevented from settling out. The metallic particles were shown to be alloys of zirconium with interfering elements, e.g. Al, Si, Mn, Fe, etc., and to arise both from residual traces of these elements in the magnesium used and of their reducible compounds in the alloying salt. Emley reports that exhaustive attempts both to avoid the formation of these clusters of particles and to remove or decontaminate them with various fluxing methods were fruitless.

As reported earlier,²⁰ no significant flux inclusions were encountered in castings prepared at the Canadian Bureau of Mines, in spite of the fact that chloride mixtures were used for the preparation of the alloys. Many hundreds of cast test bars and some other castings were stored for periods up to 40 months (for a study on room-temperature aging) and showed no visible flux inclusions. Preliminary corrosion tests showed also no flux contaminations. It is possible, however, that our melts were too small or that the British findings referred to large billets and heavy sections only.

There remains also another explanation. As mentioned already, almost all of our work was based on high-purity magnesium, prepared by the ferrosilicon process. Table 4 shows typical analyses of commercial grades of magnesium produced by an electrolytic method in Great Britain, as quoted by Emley, 18 and of Canadian high-purity magnesium, as used in our work. Only elements "objectionable" in Mg-Zr alloys are listed. The statement by Emley, 18 that the iron content in magnesium produced by the ferrosilicon process "is increased roughly to the electrolytic figure on remelting and refining in steel crucibles," is not borne out in Canadian practice 13,21 where the iron pick-up under proper foundry techniques is negligible.

TABLE 4-TYPICAL CHEMICAL ANALYSES OF COMMERCIAL MAGNESIUM

| Content, % | Al | Fe | Mn | Si | Ni | P |
|-----------------|-------|-------|--------|-------|--------|--------|
| Electrolytic Mg | | | - | | | |
| (British) | 0.005 | 0.030 | 0.150* | 0.005 | 0.001 | 0.025* |
| High-purity Mg | | | | | | |
| (Canadian) | 0.004 | 0.002 | 0.002 | 0.006 | 0.0005 | 0.001 |

* Maximum figures using unfavorable raw materials.

It is obvious that, using high-purity magnesium, the residual objectionable elements, especially manganese, cannot be as active as in commercial electrolytic magnesium. It is therefore suggested that the lack of chloride inclusions in our ZK61 alloy castings is, at least partly, due to the use of high-purity magnesium. The most important factor in obtaining flux-free, high-quality castings, in whatever metal or alloy, is careful and controlled foundry technique. The authors agree with Emley¹⁹ that flux inclusions in magnesium alloy castings are not due to inefficiency of the flux used or to improper fluxing procedure, but are caused mainly by faulty pouring technique or improper mold preparation.

Zinc Content

To establish the most favorable range of zinc in Mg-Zn-Zr alloys a series of experimental alloys was cast with zinc contents from 1 to 11 per cent and over 0.7 per cent zirconium. Figure 3 shows the effect of varying zinc contents on the tensile properties of as-cast

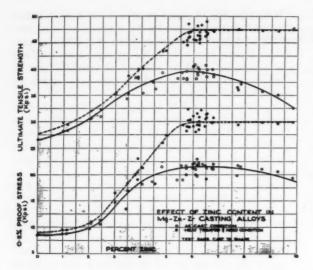


Fig. 3-Effect of zinc content on mechanical properties.

and fully heat treated (2 hr at 500 C, air cooled, 48 hr at 150 C) conditions.

The diagram shows clearly why ZK61 is considered the most outstanding alloy of the Mg-Zn-Zr system. This alloy combines the highest as-cast ultimate tensile strength and proof stress with high elongation (up to 14 per cent), excellent amenability to heat treatment and room temperature aging, and good castability. Higher zinc contents cause some drop in the as-cast properties and are more difficult to cast.

Alloys with lower zinc contents show remarkable elongation values, up to 25 per cent at 1 to 2 per cent Zn; above 7 per cent Zn, the elongation drops to 3 to 5 per cent in 2 in.

Additions of Other Elements

A detailed account of all results on ternary and quaternary Mg-Zr alloys containing other additions than zinc, would be outside the scope of this paper.

In general, it may be stated that: cadmium additions to Mg-Zn-Zr alloys increase the mechanical properties; copper additions decrease somewhat tensile strengths and lower considerably the maximum solution heat treatment temperature; calcium additions to ZK61 cause considerable decrease of all tensile properties and extreme brittleness; beryllium additions tend to lower somewhat the tensile properties due to grain coarsening but show some promise and should be further investigated.

Cerium additions are known to improve considerably the elevated temperature properties of magnesium alloys. Considerable work on Mg-Ce-Zr alloys was reported by Murphy and Payne,²² and Mg-Zn-Ce-Zr alloys were studied in the United States²⁸ and in Great Britain.²⁴ Some results of experimental work on similar alloys at these Laboratories are being reported separately.²⁵

Small cerium additions to Mg-Zn-Zr were reported²⁶ to decrease considerably the tendency to microporosity in Mg-Zn-Zr alloy castings and to be used in pressure-tight castings. Table 5 shows some preliminary results obtained on small cerium additions to alloy ZK61. It reveals that increasing cerium contents cause improvement of the fluidity of the alloy but decrease considerably mechanical properties, especially in the HTA condition.

Experimental Heat Treatment

The original patents²⁷ on zirconium-containing magnesium alloys specified, for ternary alloys, a heat treatment in the range of 150 to 400 C (300 to 750 F). It was claimed in these patents that a preliminary homogenizing heat treatment was not only unnecessary, but caused "considerable deterioration of the mechanical properties" due to the formation of "extremely coarse" grain structure.

Beck¹⁰ quotes in his book a heat treatment used for the German alloy ZB (3 Zn, 0.5 Zr) consisting of a 5 hr anneal at 280 C (536 F). Ball states⁶ that best results for a sand casting alloy of 4.5 Zn and 0.7 Zr are being achieved after heat treatment of 24 hr at 180 C (356 F). Magnesium Elektron Ltd. specifies in its 1947 Handbook for "Elektron Alloy Z5Z" a heat treatment for 24 hr at 200 C (397 F).

Mechanical properties obtained using these heat treatments, as quoted by the above authors, are shown in Table 6 which lists also, for comparison, typical results for alloy ZK61 achieved at the Canadian Bureau of Mines with a two-stage heat treatment.¹¹

TABLE 6-DEVELOPMENT OF HEAT TREATED MG-ZN-ZR
CASTING ALLOYS

| | | | | | | Typi | cal Prope | rties |
|-------|---------------|-----------|---------|------------|---------------|---------------|-----------|-------|
| Alloy | Nomi Comp. | % | Time | | UTS | 0.2% PS | | |
| ZB | Zn 3 | Zr 0.5 | hr 5 | °C 280 | kpsi 30-40 | kpsi 21-25 | kpsi | 2 in. |
| Z5Z | 4.5 | 0.7 | 24 | 180 | 36-40 | _ | 21-23 | 7- 9 |
| ZK61 | 6 | 0.8 | 2 48 | 500 150 | 44-48 | 28-32 | 26-30 | 8-12 |

TABLE 5-EFFECT OF SMALL CERIUM ADDITIONS (Each value is an average of 3 results)

| | .C | omposition, | % | | As Cast | | | HTA | | | |
|-------------|--------------|-------------|------|-------------|-----------------|-------------------|-------------|-----------------|-------------------|-------------------|--------------------|
| Melt No. | Zn | Zr | Ce | UTS kpsi | 0.2% PS kpsi | Elong. % on 2 in. | UTS kpsi | 0.2% PS kpsi | Elong. % on 2 in. | Size 0.001 in. | Fluidity Spiral |
| 1543A | 6.37 | 0.76 | - | 40.6 | 21.7 | 10.0 | 47.0 | 29.5 | 9.5 | 1.9 | 20 in. |
| 1543B | 6.11 | 0.70 | 0.12 | 39.2 | 21.4 | 9.5 | 45.0 | 28.8 | 6.0 | 1.9 | |
| 1544 | 6.03 | 0.71 | 0.27 | 38.8 | 22.0 | 9.3 | 44.0 | 28.1 | 7.5 | 1.8 | 21 in. |
| 1546 | 6.32 | 0.73 | 0.48 | 36.3 | 21.8 | 6.7 | 39.7 | 26.2 | 5.5 | 1.8 | 22 in. |
| 1547 | 6.37 | 0.71 | 0.64 | 34.2 | 21.0 | 6.3 | 39.3 | 25.9 | 6.5 | 2.0 | 24 in. |
| 1549 | 6.01 | 0.75 | 1.06 | 32.0 | 20.0 | 5.8 | 35.3 | _* | 5.0 | 1.8 | 28 in. |
| | * Bars warpe | | | | | | idity spir | al cast into | green san | d at 760 C | (1400 F) |

The above heat treatments were tried out in the first stages of the present investigation and the results achieved on various Mg-Zn-Zr alloys were generally slightly better than previously published values. Further experimental treatments were carried out to establish the most advantageous aging treatment to improve the mechanical properties. A long series of heat treatments including variations in temperature from 100 to 200 C (210 to 390 F), and aging times up to 14 days, showed that prolonged aging at low temperatures produces the highest proof stress. The results established that the optimum properties are obtained using aging temperatures in the range of 130 to 150 C (265 to 300 F).

These first series of the heat treating experiments showed that the one-step heat treatment (aging of as-cast alloys), as recommended in European publications, raises considerably the proof stress without much effect on the ultimate tensile strength of the casting and lowers somewhat its elongation.

The next series of heat treatments were carried out to explore the effect of adding a homogenizing (solution) heat treatment at higher temperatures before the recommended aging treatment. First, the solution temperatures were kept below 400 C (750 F), in accordance with the original patent claims already quoted. The results showed definitely that a two-step heat treatment produces much higher ultimate tensile strengths and elongations than the one-step treatment.

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Some of these solution treatments were carried out just below 400 C (750 F), over periods up to 24 hr, and did not cause any grain coarsening or deterioration of mechanical properties. Since these higher solution temperatures gave increased tensile properties, the next series of experimental heat treatments included temperatures in the range of 400 to 520 C (750 to 970 F). After considerable experimentation it was established that the best and most consistent results were achieved using short solution treatments at 500

TABLE 7-EFFECT OF VARIOUS HEAT TREATMENTS Melt No. 1460: 5.80 Zn, 0.73 Zr

| Solutio | n H.T. | Ag | ing | | | | Elong. |
|-------------|--------|-------------|-------|-------------|-----------------|------|--------------|
| Time, hr | Temp. | Time, hr | Temp. | UTS kpsi | 0.2% PS kpsi | | % on 2 in |
| _ | As | Cast | | 38.8 | 22.0 | 20.0 | 9.0 |
| - | | 24 | 180 | 40.8 | 26.6 | 23.7 | 5.5 |
| | | 48 | 150 | 40.1 | 27.9 | 25.2 | 6.0 |
| 2 | 500 | 48 | 150 | 46.8 | 30.2 | 27.7 | 9.0 |
| 2 | 500 | 72 | 130 | 47.9 | 31.5 | 28.6 | 9.0 |
| 2 | 500 | 14 days | 95 | 48.9 | 33.3 | 31.1 | 9.0 |

C (932 F) followed by a prolonged aging at 150 C (300 F). Using this heat treating cycle no grain coarsening was encountered.

To illustrate some of the above experiments, Table 7 shows the effect of various heat treating cycles on the tensile properties of melt No. 1460 of ZK61 alloy. The considerable difference in effectiveness of onestep vs two-step heat treatments, as well as the higher results achieved at lower aging temperatures are evident.

Another stage of the heat treating experiments was conducted to determine the lowest solution temperature at which results comparable to those achieved at 500 C (932 F) could be obtained consistently. It was considered that lowering of this temperature would be important in the case of more complicated castings, to avoid sagging or distortion of castings. It was found that temperatures above 425 C (800 F) can be used with only slight sacrifice of mechanical properties.

Effect of cooling rate from the solution temperature was investigated by comparing the normal air cooling with quenching in oil, in hot or cold water, and with cooling down in the furnace. The results showed, as could be expected, slight increases in proof stress after quenching in oil or hot water, but were not consistent, because of some tendency to warping or internal cracking due to thermal stresses.²⁸ Cold water quenching

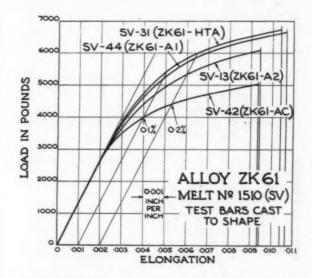


Fig. 4—Effect of various heat treatments on tensile properties of alloy ZK61.

TABLE 8-TYPICAL PROPERTIES OF HEAT TREATED ZK61 ALLOYS

Nominal Composition: 5.5 to 6.5 Zn
0.7 to 0.9 Zr
Average Grain Size: 0.001 to 0.003 in.
(Results obtained at Canadian Bureau of Mines)

| | | | Typical | Properties | | Highest Results | | | |
|-----------|---------------------------------|-------------|-----------------|-----------------|-------------------|-----------------|-----------------|---------------------|--|
| Condition | Heat Treatment | UTS kpsi | 0.2% PS kpsi | 0.1% PS kpsi | Elong. % on 2 in. | UTS kpsi | 0.2% PS kpsi | Elong. % on 2 in | |
| ZK61-AC | . As Cast | 38-42 | 20-22 | 18-20 | 9-14 | 42.0 | 23.9 | 14.0 | |
| ZK61-A2 | 24 hr at 180 C | 38-42 | 26-28 | 24-26 | 5-8 | 42.8 | 28.3 | 8.5 | |
| ZK61-A1 | 48 hr at 150 C | 40-43 | 27-30 | 25-28 | 5-8 | 43.4 | 30.0 | 10.5 | |
| ZK61-HTA | 2 hr at 500 C 48 hr at 150 C | 44-48 | 28-32 | 26-30 | 8-12 | 49.0 | 35.6 | 14.0 | |

TABLE 9-EFFECT OF ROOM-TEMPERATURE AGING

| Composition | | | As-Cast | | | | After 2 Mont | hs | After 10 Months | | | |
|-------------|------|------|-------------|-----------------|----------------------|-------------|-----------------|-------------------|-----------------|-----------------|----------|--|
| Melt No. | Zn | Zr | UTS kpsi | 0.2% PS kpsi | Elong. % on 2 in. | UTS kpsi | 0.2% PS kpsi | Elong. % on 2 in. | UTS kpsi | 0.2% PS kpsi | Elong. % | |
| 1264 | 6.00 | 0.84 | 40.4 | 21.2 | 10.8 | 40.8 | 23.3 | 9.0 | 41.9 | 26.0 | 7.8 | |
| 1286 | 6.22 | 0.90 | 39.8 | 21.8 | 9.0 | 40.6 | 23.6 | 9.0 | 43.6 | 26.0 | 8.5 | |
| 1287 | 6.78 | 0.84 | 38.6 | 21.1 | 8.3 | 40.0 | 23.2 | 8.0 | 42.0 | 25.8 | 8.0 | |
| 1288 | 6.48 | 0.83 | 40.7 | 21.7 | 11.0 | 41.1 | 23.6 | 9.2 | 42.4 | 26.0 | 8.0 | |
| 1289 | 6.53 | 0.78 | 39.8 | 21.2 | 9.8 | 40.2 | 22.9 | 8.0 | 41.4 | 25.3 | 8.3 | |

was entirely unsuccessful because all test bars showed cracks. Bars cooled slowly in the furnace showed considerably reduced strength properties.

Table 8 shows the ranges of typical properties of ZK61 alloy test bars after various heat treatments, and the highest results achieved at these Laboratories.

Figure 4 shows stress-strain diagrams illustrating the effect of the above heat treating cycles on the tensile properties.

Room Temperature Aging

Hardness determinations and numerous tensile tests carried out on cast-to-shape test bars stored for a longer time showed a decided increase in these properties due to room temperature aging. A study of this problem is being continued and will be reported separately.

To illustrate the extent of these property changes, some preliminary results are given in Table 9. Figure 5 shows stress-strain diagrams of bars tested immediately after casting and after storing for two and ten months.

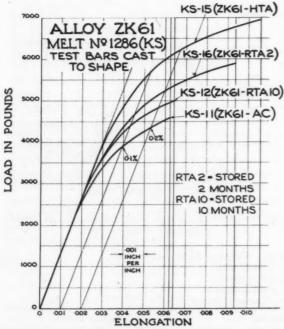


Fig. 5—Effect of room-temperature aging on tensile properties of alloy ZK61.

This study of room-temperature aging revealed that Mg-Zr and Mg-Zn-Zr alloys with low zinc contents show no changes in mechanical properties after storing

at room temperature for periods up to 3 years. Binary Mg-Zn alloys with zinc contents over approximately 2 to 3 per cent Zn show increases in UTS and PS with only slight decrease in elongation. Ternary Mg-Zn-Zr, with or without further alloying additions, show considerable increases in UTS and PS, and only small variations in elongation.

As-cast alloy ZK61 shows, after room-temperature aging, a combination of mechanical properties superior to those of any standard magnesium casting alloy in the fully heat treated (HTA) condition. This suggests the use of this alloy in the as-cast condition whenever high strength of a casting is essential, but its intricate shape or local conditions render heat treatment impossible or too expensive.

ZK61 Compared with 40E

This alloy is comparable in this respect to the American aluminum casting alloy "Frontier 40E" which ages at room temperature without previous heat treatment.²⁹ It should be added, however, that the rate of changes in tensile properties due to extended room temperature aging in alloy ZK61 is considerably slower than in Frontier 40E alloy.

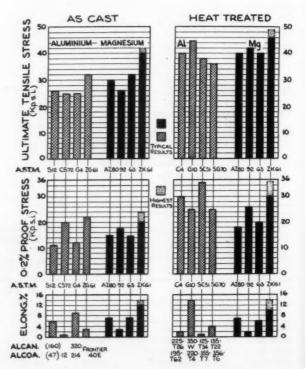


Fig. 6-Comparison of sand-cast light alloys.

TABLE 10—COMPARISON OF ALLOY ZK61 WITH SOME STANDARD CASTING ALLOYS

| Alloy Designation | Те | ensile Pr | Strength Weight Ratio | | | |
|----------------------|-------------|--------------------|--------------------------|----------------------|---------------------|--|
| | UTS kpsi | 0.2% PS kpsi | Elong. % on 2 in. | Spec. UTS kpsi | Spec. PS kpsi | |
| Mg | | | | | | |
| AZ63-HTA | 40 | 20 | 6 | 21.7 | 10.9 | |
| AZ80-HTA | 40 | 18 | 6 | 22.0 | 9.9 | |
| AZ92-HTA | 42 | 25 | 2 | 23.0 | 13.9 | |
| ZK61-AC | 40 | 20 | 12 | 21.7 | 10.9 | |
| ZK61-RTA | 42 | 26 | 8 | 22.8 | 14.1 | |
| ZK61-HTA Al | 47 | 30 | 10 | 25.5 | 16.4 | |
| SC51-T7 | 35 | 25 | 3 | 13.0 | 9.3 | |
| C4-T62 | 40 | 30 | 5 | 14.2 | 10.8 | |
| G10-T4 | 46 | 25 | 14 | 17.8 | 9.7 | |
| Cu | | | | | | |
| Mn Bronze (AC) | 115 | 70 | 15 | 14.6 | 8.9 | |
| Al Bronze (HTA) | 120 | 70 | 5 | 16.0 | 9.3 | |
| "S" Monel (AC) Fe | 140 | 110 | 2 | 16.7 | 13.1 | |
| Gray Cast Iron (AC) | 50 | _ | _ | 6.9 | _ | |
| Malleable Iron (HT) | 75 | 55 | 7 | 10.0 | 7.5 | |
| Low Alloy Steel (HT) | 110 | 85 | 30 | 14.0 | 10.8 | |
| AC -As Cast | | HTA- | -Heat T | reated a | nd Aged | |
| RTA-Room-T | empera | ture Age | ed | | | |

Comparison of ZK61 with Standard Casting Alloys

Figure 6 shows a comparison of typical mechanical properties of magnesium alloy ZK61 with some standard aluminum and magnesium casting alloys in both the as-cast and the fully heat treated conditions. The chart shows that alloy ZK61 is equal to any of the aluminum casting alloys even without consideration of its superior strength-to-weight ratio.

Table 10 gives a comparison of alloy ZK61 with some standard ferrous and nonferrous casting alloys. It shows that alloy ZK61-HTA has the highest strength-to-weight ratio found in any commercial casting alloy.

Summary

1. An extensive investigation of various casting alloys based on the Mg-Zn-Zr alloy system showed that optimum mechanical properties in the as-cast and the heat treated conditions are obtained using alloy ZK61 containing 6 Zn and over 0.7 Zr.

2. The use of high-purity magnesium in the production of ZK61 alloy castings allows the use of a fused chloride mixture as an alloying agent for the introduction of zirconium without excessive flux inclusions. The use of high-purity magnesium gives, also, a higher alloying efficiency because less zirconium is lost due to settling out impurities.

3. A heat treatment was developed for alloy ZK61 castings, consisting of a short solution treatment at high temperatures (425 to 500 C) (800 to 932 F) and a longer aging treatment at low temperatures (at or below 150 C) (300 F).

4. Aging at room-temperatures, without any prior heat treatment, produces in ZK61 alloy castings a combination of mechanical properties superior to those of any standard magnesium casting alloy in the fully heat treated (HTA) condition.

5. A comparison of mechanical properties of various ferrous and nonferrous casting alloys shows that the newly developed magnesium casting alloy ZK61 has the highest strength-to-weight ratio found in any commercial casting alloy.

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DISCUSSION

Chairman: R. T. Wood, American Magnesium Corp., Cleveland.

Co-Chairman: J. C. McDonald, The Dow Chemical Co., Midland, Mich.

F. P. STRIETER (Written Discussion): 1 The authors are to be congratulated for their fine work on this alloy. The information is of considerable value to the magnesium foundry industry at this time.

We agree with many of the facts which you have described in the paper, and have obtained several results which parallel yours. On the other hand, there are a few comments in this paper to which we should like to comment, or to ask for more

The solubility limits of 0.65 per cent and 0.85 per cent for zirconium in magnesium and in magnesium plus 6 per cent zinc respectively, are essentially equal to the values which we have found. The authors' statement that best tensile properties can be obtained with at least 0.7 per cent "effective" or acidsoluble zirconium is also in agreement with our results.

We wonder what differences in technique the authors refer to in their discussion of Fig. 2 when they show improved tensile properties at the same total zirconium level. Can this improvement be explained by a change in the ratio of soluble to insoluble zirconium contents? Along this same line we have found very consistent chemical analyses for soluble, or "effective" zirconium. It is our belief that these analyses are dependable so long as standardized chemical procedures are adhered to.

In what way are the authors' results more consistent when they alloy the zinc last? In order to provide for the remelting of scrap magnesium-zinc-zirconium alloys, we believe a system for alloying the zirconium last is beneficial and has worked satis-

factorily for us.

The liberation of chlorine gas from the fused ZrCl, salt mixture has not been noticed in any of our work. Did the authors mis-speak in their description of a degassing effect from this alloying agent? It has been our observation that a small amount of bubbling takes place upon the initial submergence of the fused salt into the molten alloy, but we felt that this was a decomposition of the hydrated salt since the ZrCl, is so hygro-

Several reasons are listed as possible explanations for flux inclusions in the magnesium-zinc-zirconium alloy system. We heartily agree with the authors that careful and controlled foundry technique is the most important factor in controlling the metal quality. However, we do not seem to be in agreement on the inference that the initial metallic impurities in the magnesium cause some flux inclusion troubles. We have produced more than 350,000 lb of ZK30 alloy billets, containing nominally 6 per cent zinc and greater than 0.5 per cent zirconium, during the past two years. These billets were all made from electrolytic magnesium using properly controlled techniques, and we have had no flux inclusion trouble.

Most of our recent work in the casting field has been with the composition containing $4\frac{1}{2}$ per cent zinc and greater than 0.5 per cent zirconium. In this composition, we find a slight additional improvement in properties by heat treating 11 hr rather than 2 hr at 500 C. This additional time is especially valuable when the zirconium content is on the low side of the

We use sulphur dioxide gas in our heat treating furnaces instead of an atmosphere containing CO₂. Based on tests conducted several years ago, we found the SO₂ gas to offer more

dependable protection.

In regard to room temperature aging of the zinc-zirconium alloys, we have checked the authors' data and also find that ZK61 alloy exhibits an aging effect on long-time holding at room temperature. The ZK51 alloy does not show this effect, however, to any appreciable extent after three months. Rather than to suggest the use of ZK61 in the as cast condition, we believe that this room temperature aging phenomenon requires that the alloy be artificially aged at an elevated temperature in order to obtain stability of its properties in use.

MR. MEIER (Reply to Mr. Strieter): I want to thank Mr. Strieter for his contribution and his kind comments.

In reply to his first question regarding the improvement of mechanical properties shown in Fig. 2, I would like to repeat that the dotted lines in the graph represent results obtained in the first stage of the investigation some three to four years ago. We believe that the considerable increases in property values were due to constant improvements of various phases of the foundry operations, especially of the alloying and casting techniques. The effects of these improvements were a more consistent grain size due to the increased "effective" zirconium contents and higher properties due to better quality (soundness) of the castings. We found, also, that the ratio of soluble (or "effective") to insoluble zirconium affects the properties of the alloy. We are using the same chemical procedures as are used in Mr. Strieter's laboratory. Some of our castings were tested there and results of the chemical analyses checked very closely. Nevertheless, we found that, especially in more complex alloys or in cases of some local segregations, etc., the grain size and the mechanical properties are much more consistent and reliable in the evaluation of the casting than a single analysis.

The next question concerns the addition of zinc. The method given in our paper is being used only for alloying of pure magnesium. In the remelting of Mg-Zn-Zr alloy ingots or scrap normally no zinc addition is necessary and only a small amount of zirconium salt is added to make up for the loss during remelting. Our statement that more consistent results were obtained when adding zinc last is based on experience. It may be that this is due to the fact that the zirconium, added first, has already completed its purifying action of the magnesium melt; the excessive amount of insoluble zirconium is finely dispersed throughout the melt and enters more easily into solution with magnesium when its solubility limit is increased due to the zinc addition. As mentioned in the paper, the zinc addition increases the solubility of zirconium in magnesium from approximately 0.65 per cent to approximately 0.85 per cent. Other factors could be the greater chemical activity of the unalloyed magnesium in reducing the zirconium salt mixture and the greater solubility of zirconium in zinc than in magnesium.

Another question deals with the liberation of chlorine gas during the alloying operation. We did not say that chlorine is escaping from the melt but we stated that the fused salt mixture decomposes and acts as an efficient fluxing agent. We agree with Mr. Strieter's statements regarding flux inclusions in ZK61 alloy castings. Our discussion of this problem is based, as mentioned in the paper, on results and conclusions reported by Emley. We expressed the opinion only that it is somewhat easier to produce flux-free castings using high-purity magnesium and that, in this case the alloying efficiency of zirconium is higher.

The heat treating schedules mentioned in our paper are recommended for casting alloy ZK61. For alloys with other zinc contents the most efficient heat treating cycles are different and have to be based on solidus temperature determinations for each particular alloy. For alloy ZK41 we found that higher solution heat treating temperatures (up to 550 C, 1022 F) may be used instead of the longer heat treatment, mentioned by Mr. Strieter.

We agree that sulphur dioxide gives a better protection in furnaces used for solution heat treatment of magnesium alloy castings. Unfortunately, most Canadian foundries are using their furnaces for a variety of other metals and, therefore, cannot tolerate the sulphur dioxide atmosphere. In our laboratory work we are using carbon dioxide and have never had any difficulties.

¹ Asst. Dir., Process Dev., Magnesium Division, The Dow Chemical Co., Midland, Mich.

With regard to room temperature aging of the Mg-Zn-Zr alloys, we found that the property changes caused by aging are primarily due to the zinc content. We agree, therefore, that aging effects are more pronounced in alloy ZK61 than in alloy ZK41, but our investigation showed definitely that the properties of casting alloy ZK41 are affected by room temperature aging. To illustrate this, I would like to quote some results obtained from our experimental melt No. 1069 (3.77 Zn, 0.79 Zr).:

| | UTS (kpsi) | 0.2% PS (kpsi) | Elong. % |
|---------------|------------|----------------|----------|
| As-cast | 38.4 | 20.0 | 12.9 |
| Stored 7 mon | 39.1 | 22.9 | 10.0 |
| Stored 17 mon | 40.5 | 24.3 | 9.8 |

In connection with the remark regarding stability of properties of ZK61 alloy castings without heat treatment, I would like to mention that up to now we could find no signs of significant dimensional changes of castings due to room temperature aging.

J. H. Schaum: ⁹ This subject of soluble and insoluble zirconium is one that has caused us much difficulty in our work as it did to Mr. Meier. I think that if he could plot the true soluble zirconium against the physical properties, he would not find as much scatter as he is finding from the use of total zirconium content.

As Mr. Meier knows it is the dissolved zirconium which is doing the grain refining and improving the properties. Fortunately, we have available at the Bureau of Standards a chemistry research laboratory which has been doing our soluble zirconium analyses for us, and the Dow Chemical Co. has kindly given us their procedure. Our laboratory experienced some difficulty with the zirconium determination. They revised it slightly in some of the last steps. I believe it has increased the accuracy and correlation we have been able to get with our soluble zirconium data.

I wondered if Mr. Meier found any changes in the microstructure as a result of his heat treating and aging. We know that the magnesium-zirconium system itself is a very sluggish alloy system which tends to precipitate metallic zirconium if given enough time at the proper temperature. We found a decrease of 50 per cent in soluble zirconium at the end of 100 hr at 500 C. Further heat treatment will be necessary to determine the end point of this trend.

It would be interesting to correlate Mr. Meier's changing physical properties with the soluble zirconium content.

MR. MARTINSON: In reply to the first question on soluble and insoluble zirconium, the three methods of analysis, used at pres-

ent, are based on hydrochloric, sulphuric, or nitric acid solutions. We have made several checks using these three methods and can find no significant differences in the results obtained from whatever method we use. As previously mentioned, checks of our results of chemical analyses of the zirconium-containing alloys by other laboratories were very satisfactory. Use of "total" zirconium content in plotting the graph in Fig. 2 was not due to any difficulties in obtaining analyses for "soluble" zirconium but it was intended to emphasize the importance of proper foundry techniques and their effect on the casting quality. It is normal in development work of this kind to discard preliminary results but we preferred to show them as an illustration of the problems encountered.

The question of changes in the ratio of soluble and insoluble zirconium during longer heat treatment has been investigated by Sauerwald¹ on binary Mg-Zr alloys, and he reported considerable decrease of the amount of soluble zirconium after long-time heat treatments. In the sand-cast ternary Mg-Zn-Zr alloys we found no significant changes in the amount of soluble zirconium after any of the heat treatments mentioned in our paper. Aging treatments at various temperatures up to 30 days of as-cast or solution heat treated bars caused no changes in the composition. A

further study of this problem is underway.

MR. STRIETER: I would like to comment further on degassing with zirconium tetrachloride. I believe that the fume given off while alloying ZrCl, to magnesium is hydrochloric acid gas, and that its presence is due to the fact that some of the zirconium tetrachloride has become hydrated. This chloride is very hygroscopic and picks up moisture readily. For the first few minutes after the zirconium tetrachloride flux is put into the melt there is a bubbling action and the bubbles contain HCl. I do not believe this bubbling action would necessarily remove hydrogen from the melt, in fact, I would suspect it may put hydrogen into the melt. You would not find hydrogen in the alloy, but I think it may lower slightly the zirconium content. There definitely is an HCl smell rather than a chlorine smell.

K. E. Nelson: ^a I noticed in your Table 8 that you give under the listing of "highest results" a tensile strength of 49,000 psi, a 0.2 per cent proof stress of 35,600 psi, and an elongation of 14 per cent. Are these the results obtained on one test bar?

MR. MEIER: These are highest results achieved in all tests for this particular property. The values given are not necessarily from the same bar.

² Metallurgist, National Bureau of Standards, Washington, D. C.
⁸ Research Engr., Dow Chemical Co., Midland, Mich.

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